SPECIFICATION AND DESIGN

OF

DYNAMO-ELECTRIC MACHINERY

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BY

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PREFACE

THE books on the design of dynamos are so numerous and so excellent, that a serious apology is necessary for adding another to our crowded shelves. When the author was asked to write a book on Design for Messrs. Longmans' Electrical Engineering Series, he was in doubt whether he should take up the task. There appeared, however, to be no book of precedents of electrical specifications analogous to the famous "Conveyancing Precedents" compiled by Prideaux, which are so widely used by lawyers; and it occurred to the author that such a book would be of some use to those engineers who have from time to time to draw up specifications for the purchase of electrical machinery.

But a book of precedents alone would be incomplete unless it showed how the specifications might be fulfilled in the factory; and the author therefore proposed to add to each specification a worked-out design, showing at least one method of meeting the prescribed conditions. In order to do this, it has been necessary to give in the first part of the book a collection of simple rules for calculating the dimensions and quantities met with in dynamo-electrical machinery. The general method of design is that employed by many of the engineers of the Westinghouse Companies of America and Great Britain, who learnt it from Mr. B. G. Lamme. The advantages of the method are set out on pages 7 and 8. Many additions and refinements have been made by various users, so that the rules given are very much more complicated than in the original scheme; but the beauty of the method is that these refinements can be used or not, according as the time available for the work is long or short. Most commonly a calculation sheet, instead of being filled up like those given in the text, contains only a dozen figures or so, which represent the design sufficiently well for the purpose of quoting a price.

It would take many years to compile a satisfactory book of precedents; for it is only by actual experience with the requirements of machinery intended to work under the many conditions met with in practice, that one can foresee all the qualities that should be asked for from the maker. For this reason, as a

first attempt the author has confined himself to some of the more usual types of machines, and has left for future consideration specifications relating to the more special machinery required in mines, rolling mills and factories.

As the book has already exceeded considerably the size originally planned, a great deal of information which is commonly found in books on design has been intentionally omitted; and only those tables are included which contain information not so easily accessible elsewhere.

The author is indebted to Mr. V. M. Allen for most of the matter contained in Chapter VII. on the design of armature coils and formers; to Mr. K. Faye-Hansen for Figure 312; to Mr. S. C. Nottage for Figure 405; to Dr. W. Petersen for permission to use Figures 217, 220, 221, 302, 306, 307 to 309, 311, 315, 316, 348, 349, 354 to 358, 361, 376 to 378, 402, 404 and 411 from his book *Wechselstrommaschinen*; and to Dr. E. Rosenberg, Mr. J. W. Schroeder, and Mr. Robert Townend for valuable criticisms and suggestions. He also wishes to thank Mr. David Isaacs for his indefatigable proof-reading and the preparation of the Indexes. A great number of the drawings have been specially made for the book by Mr. J. Mitscha. Lastly, the author wishes to express his thanks to the British Westinghouse Electric & Manufacturing Company, Limited; for their permission to publish some of the information contained in the book.

MANCHESTER, *June* 1915.

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SYMBOLS

SYMBOL		PAGE
A_g	= area of working face in cms. $=2\pi rl$	5
A_g''	= ,, ,, in inches	6
$A_g\mathsf{B}$	= total maximum flux of whole frame	6
a	= $\Sigma mr^2 \div 981$	356 and 601
$2a$	= number of armature circuits in parallel	512
a_v	= average overhang of coils in cms.	388
B	= magnetic flux-density in C.G.S. lines per sq. cm.	5
B''	= magnetic flux-density in lines per sq. inch	6
B_c	= B in gap necessary for good commutation	480
B_g	= flux-density in the air-gap	308
B_h	= coefficient of the h^{th} harmonic in the expansion of B_J	311
B_K	= Kapp lines per sq. inch	6
$\mathsf{B}_{\max}$	= maximum magnetic flux-density per sq. cm.	49
b	= width of slot	79
b_p	= breadth of brush increased in ratio $d_a \div d_c$	479
c	= distance from corner of pole to neutral line	14
c	= number of paths in parallel	24
c	= drop of core below bore of iron	162
c_p	= width of commutator bar increased in ratio $d_a \div d_c$	479
D	= diameter of armature in cms.	154
D''	= diameter of armature in inches	299
D_m	= diameter of armature in metres	479
D_r	= greatest deflection of rotor in inches	405
d	= depth of winding in cms.	239
d_c	= diameter of commutator	479
E	= electromotive force in volts	4
E	= voltage of network	339
E_a	= voltage to star-point	429
E_t	= terminal voltage	342
e	= instantaneous E.M.F. generated	306

SYMBOL		PAGE
e_e	= evanescent voltage	128
e_s	= voltage after continued short circuit	128
f	= depth of conductor in cms.	147
f_0	= frequency of oscillation	346
G	= kilograms mass of flywheel	341
g	= length of air-gap between pole and armature	58
H	= intensity of field	36
H_x	= $\sqrt{h^2+k^2}$	543
h	= height of slot	79
h	= number of the h^{th} harmonic	307
h	= ratio of A.C. power to C.C. power in rotary converter	543
h_c	= height of conductors	79
h_d	= cooling coefficient, watts per sq. cm. per degree difference of temperature passing from surface cooled by a draught of air	229
h_e	= cooling coefficient for ends of coils	233
h_l	= cooling coefficient for sides of coil	233
h_v	= cooling coefficient for ventilating ducts	211
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h_p	= total height of pole	238
I	= electric current in amperes	218
I_A	= armature current	7
I_a	= current per conductor	8
I_aZ_a	= current loading	8
I_d	= current density in amperes per sq. cm.	227
I_e	= magnetizing eddy-current	128
I_f	= field current	299
I_K	= power factor = power factor on short circuit	299
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I_{nl}	= current per phase supplying no-load losses	420
I_0	= short-circuit current	342
I_{sc}	= short-circuit current	421
I_{sh}	= instantaneous current flowing when generator is short circuited	131
I_t	= termina amperes	512
I_u	= current flowing in alternator for unit displacement of the pole centre	339
I_x	see page	239
i_n	= thickness of insulation per cm. of depth of winding	239
K_a	= Carter's coefficient for flux fringing from poles	17
K_d	= Field's coefficient = ratio of actual loss in conductor to loss there would be if no eddy-current	146
K_e	= electromotive force coefficient	5

SYMBOL		PAGE
K_f	= flux coefficient	16
K_g	= air-gap coefficient	65
K_h	= hysteresis coefficient	47
K_k	= heat conductivity of iron punching in calories per second per sq. cm. per ° C. per cm.	253
K_L	= leakage coefficient for end-windings	388
K_m	= number of commutator bars	512
K_0	= output coefficient	447
K_q	= internal displacement coefficient	294
K_r	= regulation coefficient	299
K_s	= space coefficient = ratio of iron + air space to iron space	71
K_z	= zigzag leakage coefficient	424
K_ϕ	= cross-magnetizing coefficient	342
k	= ratio of wattless current to power current at unity efficiency	543
k_h	= heat conductivity of insulation in watts per sq. cm. per ° C. per cm. of path	239
L	= inductance in henries	129
L_c	= flux leaking across to the commutating pole and back again	480
L_k	= flux leaking from top of teeth along air-gap	480
L_n	= effective flux crossing body of slot	480
L_s	= flux encircling end-connections of armature coil	480
L_t	= sum of leakage fluxes per cm. of iron	480
l	= axial length of working face in cms.	5
l	= length of bobbin in cms.	239
l_1	= coefficient of self-induction	129
l_a	= length of path through armature core	55
l_e	= effective axial length of pole	328
l_p	= pitch of poles	426
l_t	= length of turn	161
l_y	= length of yoke	55
l_z	= length of teeth	55
M	= magnetomotive force in C.G.S. units	36
M_P	= magnetic potential	58
m	= width of mouth of slot	79
m	= number of slip-rings of rotary converter	543
N	= total magnetic flux per pole	4
N_s	= number of slots in armature	512
n	= revolutions of armature per second	5
n	= frequency in cycles per second	49
n_d	= frequency of disturbance	339
n_s	= frequency of phase-swing	338
p	= smallest pitch of slot on cylindrical surface	158
p_1	= coefficient controlled by heat gradient	238

SYMBOL		PAGE
$2p$	= number of poles	299
p_p	= pitch of poles	328
p_s	= pitch of slots	65
Q	= slots per pole	309
Q_d	= disturbing torque	339
Q_s	= synchronizing torque	339
q	= ratio $Q_s \div Q_d$	339
R	= resistance in ohms	129
R_{pm}	= revolutions per minute	6
R_{ps}	= revolutions per second	339
r	= radius of armature	5
r_1	= resistance per phase of stator	418
$r_{2,1}$	= apparent resistance of secondary referred to the primary circuit	128
r_2	= resistance per phase of rotor winding	428
r_A	= apparent resistance per phase of stator and rotor	428
r_c	= radius of coil	233
S	= breadth of phase-band	306
S	= number of turns per pole	299
S_1	= turns of primary	428
S_2	= turns of secondary	428
s	= width of slot	64
T	= total turns in series	310
T_c	= turns per coil	306
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t	= thickness of sheet in cms.	49
t	= thickness of insulated coil	158
t_c	= thickness of copper strap at right angles to B	144
V	= voltage	141
v	= velocity in cms. per second	306
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W_h	= hysteresis loss in watts per cu. cm. of iron	47
W_n	= no load losses in watts	420
W_r	= weight of rotor in lbs.	405
X	$= \frac{h}{2} \frac{S}{\tau}$	308
x	= distance from hottest part in cms.	227
x_a	= apparent reactance per phase	428
Y	= apparent impedance	428
y	= throw on commutator	512

SYMBOL		PAGE
Z	= total slots in periphery	309
Z_a	= effective number of conductors on armature	25
Z_s	= total number of conductors in series	6
Z_T	= total number of conductors. $Z_T \div c = Z_a$	24
α	= angular displacement of centre line of pole from uniformly rotating vector	339
α	= angle of slope of tip	79
β	= $I_u \div I_l$	341
γ	= slot pitch	309
ϵ	= base of Napierian logarithms	128
η	= hysteretic constant	47
η	= amplitude of tooth ripple	316
θ	= angle of displacement	305
λ_d	= permeance of body of slot per cm. length of iron	422
λ_m	= permeance of mouth of slot per cm. length of iron	422
λ_z	= permeance of zigzag path per cm. length of iron	424
λ_k	= heat conductivity in centimetre measure	221
λ''_k	= heat conductivity in inch measure	221
μ	= permeability	48
σ	= coefficient used in connection with air-gap coefficient	64
σ	= copper space factor	239
σ	= displacement on clock diagram showing electrical relations	339
σ	= angle subtended by half coil breadth $= \frac{S}{\tau}\frac{\pi}{2}$	306
Σmr^2	= flywheel effect	339
τ	= $I_n \div I_{sc}$	421
τ	= pole pitch	305
ϕ	= flux interlinking a coil	306
ϕ	= angle of lag of current	543
ϕ_e	= end-leakage per pole per ampere in stator	426
ϕ_i	= flux leakage per pole across iron teeth per ampere in stator	425
ϕ_l	= leakage flux per pole per ampere in the stator	421
ϕ_p	= normal flux per pole	421
ψ	= angle between centre line of pole and current vector	294

PART I

SHORT RULES

TO BE USED IN THE

DESIGN OF DYNAMO-ELECTRIC MACHINERY

CHAPTER I.

INTRODUCTION.

General scope of the book. The term "dynamo-electric machinery" will be here taken to include: alternating-current generators and motors, continuous-current generators and motors, and machines for converting from one kind of current to the other.

It will be assumed that the reader is familiar with the laws of electricity and magnetism as applied to the design of dynamo-electric machines and that he is conversant with the theory and operation of these machines as given in the many excellent text-books on these subjects.

It has been thought that, amongst the many books on design, there is still room for one which views the subject more particularly from the manufacturer's point of view. The problem constantly before the manufacturer is how to build economically a machine which will fulfil prescribed guarantees. This book, then, will aim mainly at giving concise methods of designing machines to meet given specifications.

The function of the performance specification. In order to treat satisfactorily of the methods of meeting guarantees, it will be well to deal with the specification itself and of the conditions of operation which must be kept in view when the specification is drawn up. There are many different circumstances under which machines are to be operated. For instance, some alternators are intended to form part of a small isolated plant and to supply a power load, others to take the mixed lighting and power load of a large central station: some motors are intended to work cranes out of doors, others to drive machinery in hot mines. It is for the user or his consulting engineer in the first place to decide what characteristics a machine shall have when it is intended to operate under certain conditions. The question then arises, how should the performance specification be worded, in order to specify a machine fitted for a particular class of work? This is a question for the purchaser's adviser. Sometimes the manufacturer acting in the capacity of advising engineer decides this question.

Secondly, if we have before us a specification and know what the machine is intended to do, what is the most economical way of building a machine to comply with the specification and give satisfaction to the purchaser? That is solely a question for the manufacturer.

It is our purpose to consider the various conditions under which each class of machine may have to operate and to give some typical performance specifications, drawn up to meet common conditions. The design of a machine will then be completely worked out to meet each specification, and notes will be given as to how possible variations in the specification could be met. In all this we must have regard to commercial requirements and the adherence to standard rules and to the utilization of standard frames.

Rules for calculation applicable to all dynamo-electric machines. Before examining each class of machine in detail it will be well to deal with certain matters which are common to all generators, motors and converters, matters relating to the magnetic circuit, the electric circuit, the insulation, the ventilation and the framework. A great number of rules, formulae and details of shop practice on these matters are common to all machines, and it will save time to dispose of them in a few preliminary chapters.

It is well to have one general method of designing all the machines, A.C. generators, C.C. generators, induction motors and rotary converters, so that the experience gained with one class may be readily available for the improvement of another. That such a general method of design is possible can be seen from the following considerations.

One general method for all machines. All dynamo-electric machines depend for their operation upon the same fundamental facts—firstly, the fact that when a conductor is moved across a magnetic field there is generated in it an electromotive force, and secondly the fact that when an electric current flows along a conductor in a magnetic field, the conductor is subjected to a mechanical force. The calculation, therefore, of any such machine raises such questions as the following: How much magnetic field? How much motion? How much voltage? How much current? How much force? And in addition, we have the important questions of how much heat is produced and how is that heat carried away.

Now there are two ways of looking at the fundamental phenomenon of the **generation of electromotive force**, and these have given rise to two general methods of design, both of which are commonly used.

According to one way, a certain *total flux* interlinking with an *electric circuit* changes in amount or completely reverses in a certain *period of time*, thus generating a certain *mean* electromotive force in the circuit during that time.

According to the other way of looking at the matter, a conductor of a certain *length* moves in a field of a certain *flux-density* at a certain *velocity* and generates for the *instant* a definite electromotive force.

The first of these aspects of the phenomenon leads us to speak of the *total flux* per pole, which we may represent by the letter N, and our formula for the electromotive force generated in the windings of the armature of an ordinary continuous-current generator, in which the number of poles is equal to the number of paths in parallel through the armature, is

$$E = nZN \times 10^{-8}, \qquad \text{(1)}$$

where n is the number of revolutions of the armature per second and Z is the total number of conductors.*

The second of these aspects leads us to speak of the *flux-density* in the air-gap, which we may designate by B, and then the instantaneous value of the electromotive force generated in one conductor moving at right angles to the magnetic flux with a velocity of v cms. per sec. is

$$e = v\mathsf{B}l \times 10^{-8}, \quad \text{.......................................}(2)$$

where l is the active length of the conductor in centimetres.

The first method of calculating the electromotive force has the advantage that it only deals with the total flux without troubling about the distribution of the lines of force in the air-gap, but this very feature limits its application to those cases where we are content to know the mean electromotive force generated in one alternation of the flux. The formula is therefore not so generally applicable as the second one, which gives us a more complete mental picture of what is happening under each pole.

Our fundamental formula for voltage generated. It is possible to have a combination of these methods which preserves the advantages of both. We may lead up to it in the following way:

Suppose that we have a rotor surrounded by a stator (see Fig. 1), as in an induction motor, but the flux in the gap, instead of changing from point to point

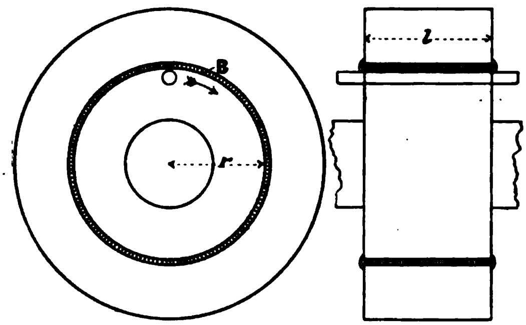

FIG. 1.—Homopolar generator with one conductor.

along the periphery, is all of one sign and distributed uniformly (the return path being, if we like, along the shaft). Consider the electromotive force generated in a conductor on the surface of the rotor when it is moved across the uniform field of the stator. If B is the flux-density in the air-gap in lines per sq. cm., r the radius of the rotor in cms., l the length of the rotor iron in cms. and n the speed in revs. per second, then the total flux passing into the rotor will be $\mathsf{B} \times 2\pi rl$ and the total flux cut per second will be $\mathsf{B}2\pi rln$. Writing A_g for the

* In the two-pole case the total change of flux through one turn in half a revolution is $2N$, because the flux changes from $+N$ to $-N$. In one whole revolution it is $4N$, thus the mean rate of change is $4nN$. Now, if Z is the total number of conductors in an ordinary two-pole drum-wound armature, the number of *turns in series* is $\frac{Z}{4}$. Thus we get

$$E = nZN \times 10^{-8}.$$

total cross-section of the gap $= 2\pi rl$, we have the electromotive force E in volts generated in one conductor,

$$E = \mathsf{B}A_g n \times 10^{-8} \quad \text{.......(3)}$$

$$\text{or} \quad E = \mathsf{B}A_g R_{pm} \times \tfrac{1}{60} \times 10^{-8}, \quad \text{.......(4)}$$

when the speed R_{pm} of the machine is given in revs. per minute.*

Observe that the formula preserves the symbol for the flux-density in the gap, and that at the same time we have $\mathsf{B}A_g$ the total flux of the whole frame clearly before us. The speed is, moreover, given in revolutions per minute instead of in linear velocity as in formula (2).

The uniform flux distribution, assumed in formula (4), does not ordinarily occur (except in homopolar machines), but it is possible to apply an equation of

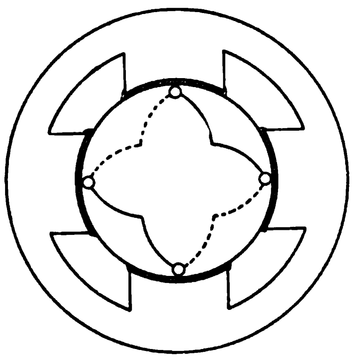

FIG. 2.—Heteropolar generator with four conductors in series.

this form to any dynamo-electric machine by introducing a coefficient so chosen as to allow for the want of uniformity in the flux distribution, and also for any peculiarities in the arrangement of the conductors. For instance, to take a simple case, assume that the stator has four poles, each of which has an effective pole arc only 0·7 of the pole pitch, as represented in Fig. 2. The average electromotive force in one conductor would be

$$E = 0{\cdot}7\mathsf{B}A_g R_{pm} \times \tfrac{1}{60} \times 10^{-8}.$$

Now if the flux is not all of the same sign, but changes from positive to negative as we go from one pole to another, and if there are Z_s conductors on the rotor, joined in series as shown in Fig. 2, the average value of the electromotive force will be

$$E = 0{\cdot}7\mathsf{B}A_g R_{pm} \times \tfrac{1}{60} \times 10^{-8} \times Z_s. \quad \text{.......(5)}$$

Note that the coefficient 0·7 would be used whatever the number of poles might be, provided that the ratio of pole arc to pole pitch were the same.

* If we prefer to work in kapp lines per square inch, denoted by B_K, the formula takes the simple form

$$E = \mathsf{B}_K A''_g R_{pm} \times 10^{-6},$$

for one kapp line $= 6000$ C.G.S. lines. Here A''_g is in square inches.

Or if we prefer to work in C.G.S. lines per sq. inch,

$$E = \mathsf{B}'' A''_g R_{pm} \times \tfrac{1}{60} \times 10^{-8},$$

where $A''_g =$ area of the gap in square inches, and B'' the flux-density in lines per sq. inch

If now the armature current be denoted by I_A, the output in watts,

$$I_A E = 0{\cdot}7 \times \tfrac{1}{60} \times 10^{-8} \times R_{pm} \times \mathrm{B} A_g \times Z_s I_A. \quad \text{......................(6)}$$

The electromotive force coefficient K_e. Now consider that the flux is not uniform under the poles but varies from point to point, having any value from 0 to B, where B is the maximum value, and that the conductors which are connected together are out of phase with one another as depicted in Fig. 3. The same form of equation is still applicable for calculating the electromotive force, provided we choose such a coefficient as will allow for the peculiarity in the flux distribution and in the arrangement of the conductors, and also, in an alternating-current machine, for the taking of the square root of mean square of the voltage, when the result is to be given in virtual volts. The exact

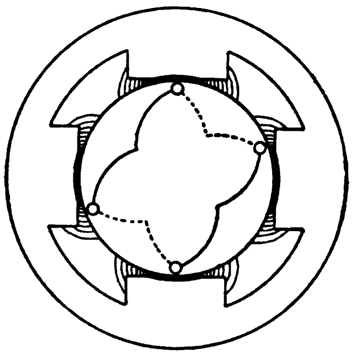

FIG. 3.—Heteropolar generator with varying flux-density and conductors out of phase with one another.

method of allowing for these things will be given in its proper place. We wish to point out here a formula of the general form

$$E = K_e \mathrm{B} A_g R_{pm} Z_s \times \tfrac{1}{60} \times 10^{-8} \quad \text{..............................(7)}$$

can be used for calculating the electromotive force of any dynamo-electric machine and that this formula has the following advantages in its favour:

(1) The formula contains the term B representing the maximum value of the flux-density in the air-gap, and this term, as we shall see later, is useful in many ways.

(2) The expression $\mathrm{B}A_g$, the maximum flux-density multiplied by the total area of the active surface of the armature, has a fairly definite maximum value for any given frame, so that if we are familiar with our frame we know by a glance at the formula to what extent we are making good use of it. For instance, if we have an armature for an A.C. generator whose diameter is 50 inches and length 10 inches, then $A_g'' = \pi 50 \times 10 = 1570$, and if we know from experience that B'' cannot be made higher than 60,000 then the maximum value of $\mathrm{B}''A_g''$ for that frame is 94×10^6.

As this quantity $\mathrm{B}A_g$ is almost independent of the number of poles, the designer soon comes to know the value it should have for any particular frame, and is able to judge at a glance how far he is utilizing the magnetic circuit of that frame.

(3) The coefficient K_e also has a certain recognized maximum value for a certain kind of machine. Thus, for a three-phase generator K_e may be equal to 0·4. If it has a lower value in any calculation under consideration (as may be the case where the pole arc is a small fraction of the pole pitch), the designer's attention is called to that circumstance.

(4) Just as the expression $\mathsf{B}A_g$ gives us at a glance the **magnetic loading** of the frame, so the expression I_aZ_a tells us at once the **current loading**. Here we have taken I_a as the current per conductor and Z_a for the total conductors on the armature. If there are a number of paths in parallel, then if I_A is the current at the terminals and Z_s the number of conductors in series, the current loading will be I_AZ_s. In using the method of design given here, the expressions $\mathsf{B}A_g$ and I_aZ_a are continually in evidence, and we can watch how one decreases and the other increases in the fight for room which occurs between the iron and the copper. The output of the frame is of course proportional to the product of $\mathsf{B}A_g$ and I_aZ_a.

All the machines dealt with in this book may be regarded as variations of one type of machine, say, of the alternating-current generator. It may be said that the differences in the design of the different types of machine consist in the amount of importance which we attach to certain features. Thus, an induction motor is a machine with a very great armature reaction, and a very small air-gap, magnetized entirely by wattless current from the line and provided with a large amortisseur.

In a generator we attach importance to having a large magnetomotive force on the magnetic circuit; while, in an induction motor, we attach importance to keeping the magnetomotive force as small as possible. In a commutating machine special attention is given to keeping the self-induction per coil as low as possible, and preserving a good field form, otherwise inside the armature it is very like an alternating-current generator.

In fundamental design all these machines are the same, and the formula

$$E = K_e\mathsf{B}A_gZ_sR_{pm} \times \tfrac{1}{60} \times 10^{-8}$$

is applicable to all.

Methods of calculation common to all machines. The calculations of the magnetic circuits of all these machines are very similar, involving, as they do, mainly considerations of the air-gap, teeth and iron body of the machine. Again, the considerations which enter into the calculations of the electric paths are very similar. The convenient kinds of windings, the calculation of the conductors and the arrangements for cooling are nearly the same for all the machines. It is therefore well to take up these general matters in a few preliminary chapters, and then when we come to consider each class of machine by itself we will be able to avoid repetition and devote ourselves to those points which relate particularly to that class.

Judicious guessing. It must not be supposed that the rules given in the subsequent chapters are intended to be employed in all cases in which they are applicable. A busy designer would never get through his work if he stopped to calculate everything. He guesses a great deal, or makes rapid mental estimates of quantities he has not time to calculate. Now, he is never justified in so

guessing unless he knows the limit of his possible error with fair accuracy, and knows that with the error he will still have a machine which will comply with its specification. Knowledge of these two things can only come from many calculations made and many machines tested. The way to acquire the art of correct guessing is to employ fairly simple rules for calculation that are based on sound principles. An empirical rule, however often applied, does not help the mind to form rapid mental estimates, because it does not take into account all the factors that determine the result.

While some of the rules here given may seem to lead to calculations which are too lengthy for ordinary shop use, it must be remembered that an hour's calculation may sometimes save the designer weeks of worrying experience. The great art is to know what to calculate and what to guess.

CHAPTER II.

THE MAGNETIC CIRCUIT.

Field-form and field-form coefficients. We shall assume that the reader is acquainted with the laws of magnetism and their application to the design of dynamo-electric machines. Our object in the following chapters will be to collect for his convenience rules which are useful in the calculation of magnetic quantities in the commercial design of machines and to emphasize those points in the magnetic design which experience has shown to be of importance. At first we will only consider those points which are common to all electrical machines whether for alternating or continuous-current.

THE EFFECT OF THE NUMBER OF POLES ON THE GENERAL DESIGN.

Different numbers of poles on an armature of given diameter. The fixing of the number of poles which a machine shall have is one of the matters to be taken up later, when we are considering each machine in its own class; but we may here look at the general effect on the design of having few or many poles, irrespective of the question whether the machine is for alternating or continuous-current or whether it is a generator or a motor.

In the first place, we know that for a given speed, given ampere-wires per inch, and given flux-density in the gap, the output of a machine is proportional to D^2l, where D is the diameter of the active face of the armature and l the length of the iron. As a first approximation, it is independent of the number of poles. Now, if we fix D we can draw a circle which represents the periphery of the active face of the armature, and we can draw out diagrammatically the magnetic circuits for a two-pole, a four-pole, a six-pole and an eight-pole machine, as is done in Figs. 4, 5, 6 and 7.

In these figures the outputs are supposed to be the same at the same speed. The diameter is constant, and for the moment we will take the air-gap the same in all cases (though in practice it would usually be greater when the poles are fewer). It will be at once seen from these diagrams that, under the conditions specified, the machine with the few poles requires more iron than the machine with many poles. The dimensions a and b are supposed to represent the depths occupied by the teeth and the windings, and the dimensions c and e are the depths

of the iron behind the slots which serve as paths for the magnetic flux. In the two-pole case (Fig. 4) the magnetic flux which threads through the rotating element has only two paths by which to return, and the depth c must therefore be made very great. Moreover, if the density in the gap is reasonably great, we will require the whole of the radius e to carry the flux. Where the rotating

FIG. 4. FIG. 5.

Diagrammatic views of two-pole and four-pole generators of the same working diameter showing the relative amounts of iron required.

element is a field magnet we can utilize the steel of the shaft to carry the flux, but where the flux is alternating the dimension e must not include the shaft (see Fig. 8), so that in the two-pole case we would be under an additional disadvantage, for we have to increase D in order to make room for e. This still further increases the total quantity of material in the machine.

FIG. 6. FIG. 7.

Diagrammatic views of six-pole and eight-pole generators of the same working diameter, showing the relative amounts of iron required.

In the four-pole case (Fig. 5), assuming the same flux-density in the gap, the depths c and e need only be about one half as great as in Fig. 4. It should be remembered, however, that where the frequency is doubled (say 50 cycles instead of 25) it is usual to work the iron at a rather lower density.

In the six-pole case (Fig. 6) the iron behind the slots is still further reduced, and in the eight-pole case the machine assumes the general proportions indicated in Fig. 7.

It is not only in the magnetic circuit that the two-pole machine takes more material than the four-pole, and the four-pole more than the six-pole. In the electric circuit also the end connections are longer and more bulky when the poles are fewer. In the above figures we have taken the speed constant, and the frequency therefore increases with the number of poles. The result is as we would expect; there is less material required at higher frequencies.

In those cases where the *frequency is prescribed* and the speed may be chosen, it usually pays to adopt a six-pole construction in preference to a four-pole, notwithstanding the fact that the speed is lower in the six-pole case. The material required for a four-pole 25 cycle machine running at 750 R.P.M. is rather less than

FIG. 8.—Two-pole machine with revolving armature having the same output, at the same speed, as the machines shown in Figs. 4 and 9.

for a two-pole machine of the same output running at double the speed. There may, however, be good reasons for adopting the higher speed, as, for instance, where a steam turbine is used for driving and the higher speed gives better economy.

With continuous-current machines, where the *speed is prescribed* and the frequency may be chosen, the number of poles sometimes depends on the desirable number of brush arms, but apart from this consideration one will not adopt a two-pole construction in preference to a four-pole construction unless the size is so small that the reduction in the cost of labour is more important than the reduction in the cost of material. The difference in the amount of material for the two-pole and the four-pole cases when the machine has inwardly projecting poles can be seen at once from a glance at Figs. 8 and 9. It is even more striking

than the cases considered in Figs. 4 and 5. In these cases, as the machines are supposed to be of the same output, the same speed and the same length of iron, it has been necessary to increase the diameter of the two-pole machine in order to make room for the shaft, which cannot carry alternating flux. The provision of

FIG. 9.—Four-pole machine with revolving armature having the same output, at the same speed, as the machine shown in Fig. 8.

sufficient cooling surface on the two-pole field coils necessitates either a very long pole limb or a great depth of winding on each pole. But these are matters which will be more properly considered under their proper headings.

THE FIELD-FORM.

Distribution of magnetic flux in the air-gap. The value of the co-efficient K_e in the equation,

$$E = K_e \mathfrak{B} A_g Z_s R_{pm} \times \frac{1}{60} \times 10^{-8}$$

depends (*inter alia*) upon the way in which the magnetic flux is distributed in the gap. We will consider at this point how the field-form may be conveniently plotted and how the coefficient K_e may be determined for various types of machines.

There are two classes of cases to consider, (1) where the magnetomotive force is created by a coil on a simple salient pole as in ordinary continuous-current machines and engine-type alternators, and (2) when the magnetomotive force is supplied by a number of coils distributed over the pole face.

Field-form under a salient pole. In the case of the simple salient pole, we have a definite difference of magnetic potential between the pole and the armature iron, so that the density in the gap at any point depends mainly on the length of the gap at that point, and where we can neglect the saturation of the iron parts, it is inversely proportional to the length of the gap.

As an example, let us plot the field-form of a rotary converter in which the armature teeth are not highly saturated. Let the pitch of the poles be 11″

measured on the periphery of the armature, the width of the poles 8″, the air-gap being 0·2″. Let the pole have a 1″ bevel such that the air-gap at the corner is 0·3″.

Take a sheet of squared paper. Near the bottom draw a horizontal line to represent part of the periphery of the armature (Fig. 10). Choosing some convenient scale, draw two vertical lines, one for the centre line of the pole and one for the neutral line, as shown. Then draw in half of the pole and air-gap, as shown by the line *RST.* Choose some convenient scale of ordinates for the flux-density, taking the maximum density in the air-gap as unity (the actual values of

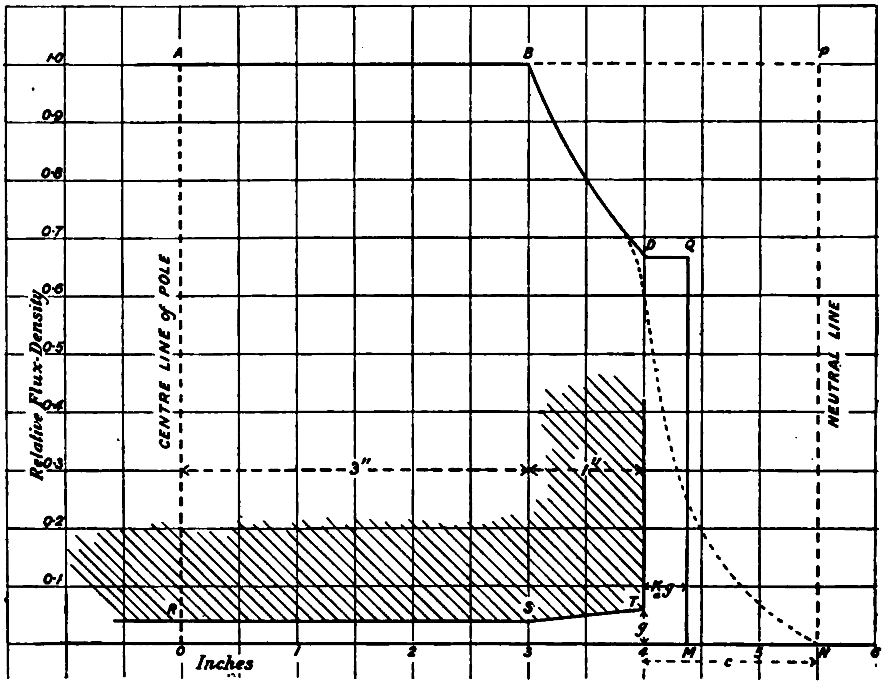

FIG. 10.—Field-form diagram for a salient pole with no saturation.

the density do not concern us for the moment). To construct the field-form, draw through ordinate 1 a horizontal line *AB*, the same length as *RS*, extending in this case 3 inches from the pole centre-line. Here the pole bevel begins. If we neglect for the moment the fringing effect, the flux-density on the surface of the armature, under the corner of the pole *T*, would be 0·66. Similarly, half-way along the bevel it is 0·8 Plot these two points, and draw the curve through them as shown. Now we must consider the fringing. The shape of the fringing curve depends mainly on two things, the length of the air-gap g at the corner and the distance c from the corner to the neutral line. Mr. F. W. Carter has given * us a method of calculating the fringing curve for any given value of c/g, and has pointed out that where we are given the fringing curves for several values of c/g we can draw by eye the curves for intermediate value with sufficient accuracy for practical

* *Jour. Inst. Elec. Engrs.*, 1900, part 146, vol. xxix. ; *Elec. World and Eng.*, Nov. 30, 1901.

purposes. In Fig. 11 are reproduced the curves which Mr. Carter has plotted for $c/g = 2{\cdot}5$, $c/g = 5$ and $c/g =$ infinity. In the case of a pole with a slight bevel, such as shown in Fig. 10, the distribution of the flux in the interpolar space will be almost the same as if the air-gap were uniform and of the same length as at the corner T, in this case 0·3 inch. For the purpose of drawing the fringing curve we must take the ordinate 0·66 as if it were the full value 1 in Fig. 11, and all other ordinates must be taken in proportion. Taking $c/g = 5$, in our case, we could, if we liked, plot the curve shown in the dotted curve and so complete the field-form; but our object is not so much to plot the exact shape of the field-form as to find its area, and it is possible to find the value of the flux between the

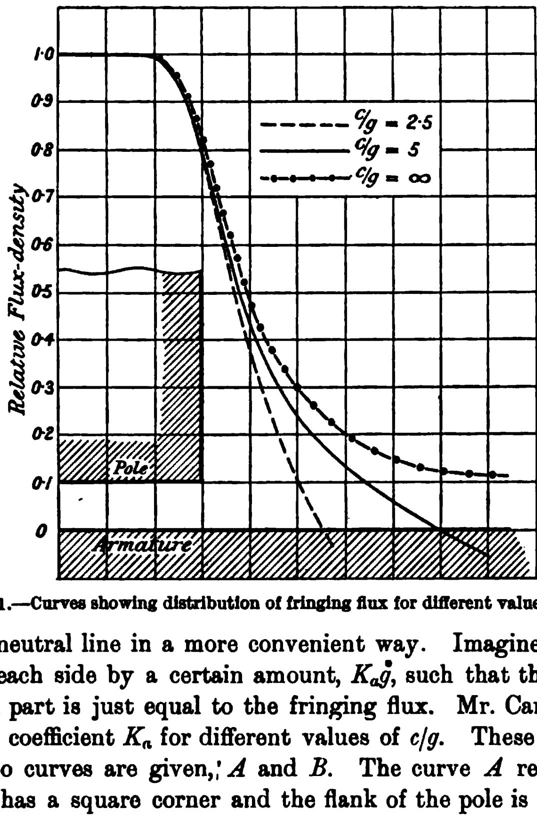

FIG. 11.—Curves showing distribution of fringing flux for different values of c/g.

corner and the neutral line in a more convenient way. Imagine that the pole is made wider at each side by a certain amount, $K_a g$, such that the flux in the gap under the added part is just equal to the fringing flux. Mr. Carter has given us the value of the coefficient K_a for different values of c/g. These values are given in Fig. 12. Two curves are given, A and B. The curve A relates to the case where the pole has a square corner and the flank of the pole is approximately at right angles to the surface of the armature. The curve B relates to the case where the pole is provided with a spur of the shape shown in the sketch at the side of the figure, there being an angle of 135 degrees between the side of the pole spur and the surface of the armature.* For any intermediate case it is easy to judge with sufficient accuracy the position of an imaginary curve drawn between A and B. Instead of plotting out the fringing curve, all that is necessary is to set off DQ, as shown in Fig. 10, and complete the flux curve as shown by the

* Messrs. Hawkins and Wallis in their excellent book on the dynamo (page 449 of the 1909 edition) give curves for various values of the angle between the pole and the surface of the armature.

full line. We obtain the length of DQ as follows: Take the ratio of c to g,—in this case 5. From Fig. 12 curve A, abscissa 5, gives us $K_a = 1{\cdot}25$. Make $DQ = 1{\cdot}25g$, in this case 0·375″. The area under DQ = area under curve DN. The area of the figure $OABDQM$ is proportional to the flux from the half pole, and the ratio of this area to the area of the figure $OAPN$ gives us a certain coefficient, which we will write K_f, the *flux coefficient*. If the maximum flux-density in the gap extended for the whole pole pitch, the flux from the pole would have a hypothetical maximum

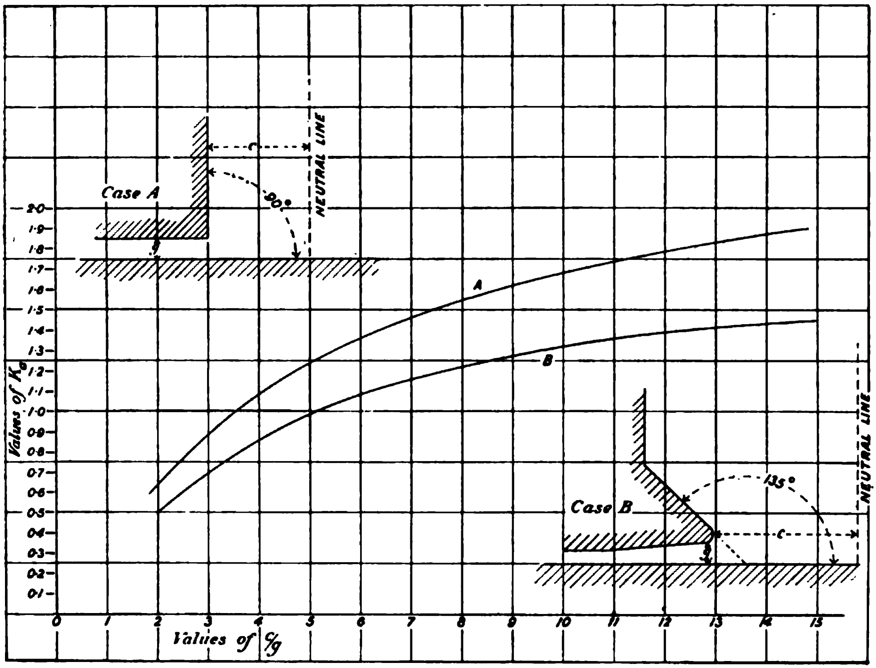

FIG. 12.—Values of fringing coefficient K_a for different values of c/g both for case A and case B.

value corresponding to twice the area of the rectangle $OAPN$. The coefficient K_f is the coefficient by which we must multiply this hypothetical maximum flux in order to get the true value of the pole flux.

Working upon squared paper, the figure can be sketched with great rapidity by hand, and taking the value of K_a from Fig. 12 we easily and accurately make allowance for the fringing flux. To get K_f, the easiest way is to run the stylus of a planimeter * around $OABDQM$, and then again around $OAPN$. The ratio of the readings gives us K_f. In Fig. 10 $K_f = 0{\cdot}738$.

* Every dynamo designer should have a planimeter at hand, because by means of it he can make quick and accurate estimates of quantities he otherwise would not take the trouble to calculate. A good plan is to work on a drawing board upon which a sheet of tracing cloth is always stretched. If the area of any figure is required, a sketch of the figure is made with a soft lead pencil, the sketch is put under the tracing cloth which serves to hold it in position and the stylus of the planimeter is run along the perimeter without any fear of that vexatious catching of the edges of the paper against the planimeter which sometimes happens when the size of the paper is not much bigger than the figure.

Cases where the saturation of the teeth cannot be neglected. Where the saturation of the teeth is fairly high, as in the case of continuous-current generators, allowance must be made for it, or the value of K_f obtained will not be sufficiently accurate.

The method of allowing for the saturation is really a method of trial and error, but where we know beforehand, as we generally do, the fraction of the total ampere-turns per pole which are to be expended on the teeth, we can get at K_f with fair accuracy by the following construction :

Consider a continuous-current generator with a pronounced pole spur (Fig. 13), the side of the spur VT making an angle of about 135 degrees with the surface of

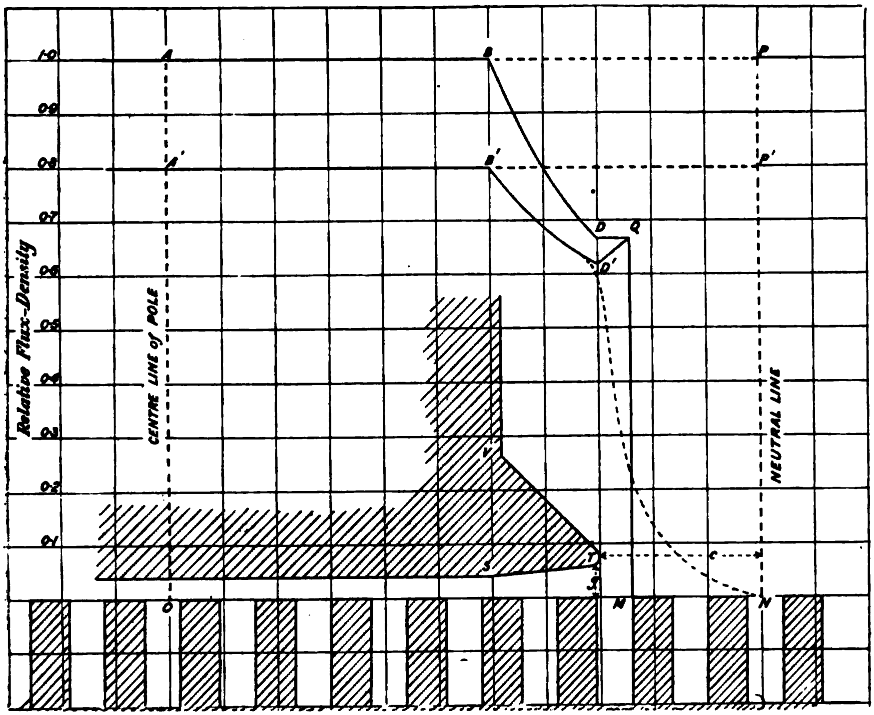

FIG. 13.—Field-form diagram for a salient pole with considerable saturation in the teeth.

the armature, and the length of VT being about the same as the length TN. This is an average case among modern generators. Even where the dimensions differ considerably from those given, the method here described will give fairly accurate results.

First neglect the saturation of the teeth, and draw the figure $OABDQM$ as before, except that instead of obtaining a value K_a from the curve A in Fig. 12, obtain it from the curve B. In the case illustrated, $c/g = 5$ and K_a therefore $= 1$. We make $DQ = 1 \times g$ and complete the figure $OABQM$.

Now, it will be seen that the effect of the saturation of the teeth will be to diminish the flux-density under the pole, while it does not affect to any great extent the distribution of the flux between the poles.

The presence of the teeth will cause the field-form to have ripples on it which move forward with the armature. In practice it is not worth while to take account of these ripples. The field-form here plotted may be taken as the average field-form with the ripples smoothed out.

Suppose that we intend to expend 20 % of the total ampere-turns on the pole in driving flux through the teeth, which are in this case somewhat saturated. Instead of having, say, 5000 ampere-turns expended on the gap, we will have available only 4000, and this will have an effect upon the general shape of the field-form, because the fringing will be about the same as if there were 5000 ampere-turns, while the flux under the pole will be diminished to 80 % of what it otherwise would be. We accordingly proceed as follows: Through the ordinate 0·8 we draw the horizontal line $A'B'$. This gives us the top of the corrected flux-distribution curve. Having regard to the amount of the bevel and the reduction in the saturation of the teeth which will occur under the corner of the pole, roughly estimate the fraction of the ampere-turns expended on the teeth under the corner of the pole. This in our case may be about 0·05. Take D' accordingly 0·05 (on the ordinate scale) below D and complete the curve $B'D'$ by hand. Join $D'Q$. Observe that the allowance for the fringing flux (the part of the figure under $D'Q$) is almost the same as if there were no saturation in the teeth. Continue $A'B'$ to P'. Then the value of K_f is obtained by finding the ratio of the area of the figure $OA'B'D'QM$ to the area of $OA'P'N$. Observe that the value of the K_f is greater in this case than if we had taken the ratio of the area $OABDQM$ to the area $OAPN$, so that the saturation of the teeth increases K_f for a given shape of pole.* The value of K_f in the case given in Fig 13 is 0·75.

One of the advantages of this method of working is that it enables the designer without any elaborate calculations to make allowances for minor matters affecting the distribution of the flux. Suppose, for instance, that the pole tip is highly saturated. If we know approximately the number of ampere-turns expended on the pole tip, we can allow for it in Fig. 13 by putting D' lower down, just as A' is put lower down, to allow for the drop in the armature teeth.

Field-form under a distributed winding. When the difference of magnetic potential between armature and field-magnet is not uniform all along the surface of the pole, as where the ampere-turns are supplied by a distributed winding, the first step is to make a diagram to give us the distribution of magnetomotive force.

Take the case of a four-pole cylindrical field-magnet whose coils lie in slots on the pole face, such as is illustrated in Fig. 371, p. 400. Let there be 96 slots, 80 wound, 4 slots being left vacant at the centre of each pole. Take the diameter at 36″ and the diameter of the bore of the armature at $37\frac{1}{4}$″, thus the radial gap is $\frac{5}{8}$″. It is best to measure the pole pitch, not on the surface of the cylinder, but half-way across the gap, that is on a circle whose diameter is $36\frac{5}{8}$ inches. The pole pitch is 28″.

There are 24 slots per pole. Lay out on squared paper a horizontal line having 25 divisions, numbered 0 to 24 (see Fig. 14); these represent 25 teeth, and the

* It must be remembered that the height of the ordinate OA is immaterial. All that we want for the moment is the ratio of the two areas named. The voltage of the machine will then be a function of the maximum density in the air-gap and the coefficient K_f.

spaces between these give us 24 slots. Mark off the 4 unwound slots in the centre of the pole. Mark off (or imagine marked off) the end connections, connecting slots 10 and 15, 9 and 16, etc. There are five teeth in the centre of the pole upon which the full ampere-turns of the coils are exerted. Represent the full ampere-turns by the ordinate 1, drawing the line AB over the five central teeth. The ampere-turns on the remainder of the teeth are less and less as we get further from the centre, and can be represented by the sloping dotted line in Fig. 14. The magnetomotive force really increases in steps but it is not worth while to take account of these. Now, if there were no saturation in the teeth the field-form

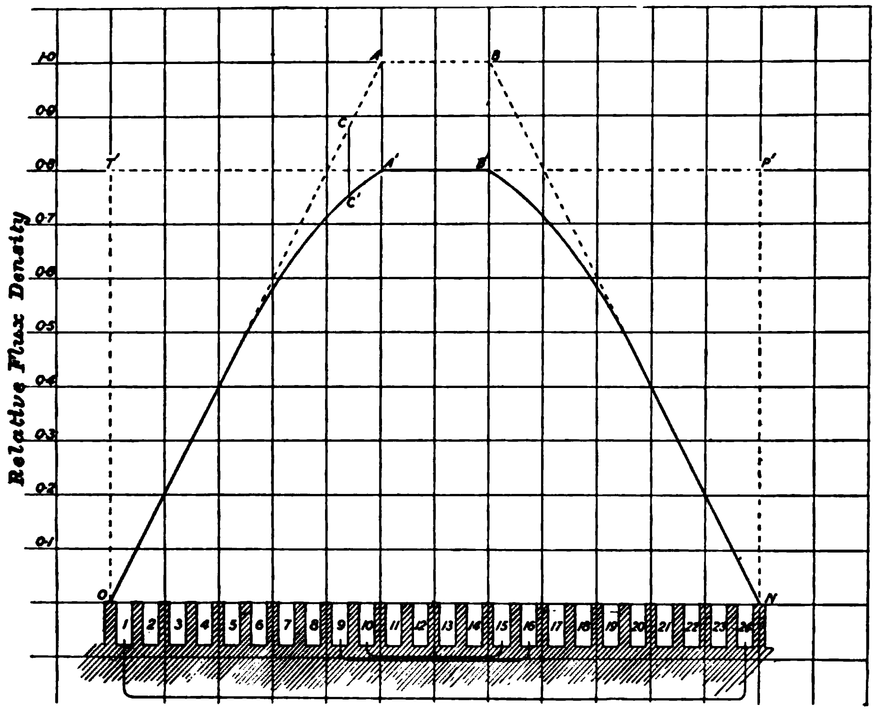

FIG. 14.—Field-form with a distributed winding and saturated teeth.

would have the same shape as the magnetomotive force curve; but in cylindrical field-magnets it is usual to saturate the teeth until they require for their magnetization a considerable percentage of the total ampere-turns. Suppose that it has been decided to expend 20 % of the ampere-turns on the central teeth. We can draw a horizontal line $A'B'$, having an ordinate 0·8 to represent the flux from the five centre teeth and complete the figure $OA'B'N$, which is of such a form that any portion of an ordinate, such as AA' or CC', represents the fraction of the magnetomotive force taken to magnetize the teeth.

The exact shape of the curve $OC'A'$ will be considered when we come to deal with the ampere-turns on the teeth.* It can, with a little experience, be drawn

* See page 78.

by hand with sufficient accuracy for the purpose of getting K_f. The ratio of the area $OA'B'N$ to the area $OT'P'N$ gives us K_f. In the case taken in Fig. 14, $K_f = 0{\cdot}65$.

THE FIELD-FORM OF INDUCTION MOTORS.

Strictly speaking, the field-form problem in an induction motor is inverted. Instead of starting with a certain magnetizing current, and then building up the field-form, and from that the electromotive force wave-form, we should, to be logical, start from the wave-form which is impressed upon the motor and work backwards to the magnetizing current. If we could do so, we should find that the current would in most cases not be at all sinusoidal, and that not only would the third harmonic be very pronounced on account of a saturation of the iron, but there would be numerous harmonics introduced to suit the particular kind of winding in the stator slots and generate in it the electromotive force like that impressed upon the machine.

Even if we knew the wave-form of the electromotive force that will be impressed upon the motor, the problem of finding the exact form of the magnetizing current would be extremely difficult. The usual practice is therefore to assume a sine wave-form for the magnetizing current and give it sufficient amplitude to create the flux required to generate the electromotive force of the motor. The designer knows that he is here making an unwarrantable assumption, and he is not surprised when the wave-form of the real magnetizing current rather upsets the calculation of the power factor of the motor. Going then into the problem with our eyes fully open to the defects in our method, we can proceed.

Take a three-phase motor, with three slots per phase per pole and a full pitch winding. Lay out the air-gap in a straight line and mark off the stator slots as in Fig. 15. It is not of course necessary to draw the slots. Assume first that the magnetizing current is at its maximum in phase A, and at half its maximum in B and C.

The numbers 1, 1, 1, 0·5, 0·5, 0·5, etc., along the top of Fig. 15 are proportional to the ampere-wires in the slots immediately above them. The tooth between the slots 6 and 7 has the maximum ampere-turns upon it, and on the middle of slot 2 lies the centre point of the band of magnetizing current.

In order to get an idea of the field-form of an induction motor, first lay out the magnetomotive force curve $ODEFGHIJN$, beginning under the centre of slot 2 and having its maximum under the tooth 6, 7. Under the centre of slot 2 the vertical line of height 1 is bisected at 0, because the ampere-turns in slot 2 may be said to be half on the pole to the right and half on the pole on the left. Under slot 3 the magnetomotive force curve rises by an amount 1 and under slot 4 by an amount 0·5 and so on, until we get down to the point N. Before we can draw the field-form we must know what percentage of the ampere-turns on the pole are expended in magnetizing the iron; this percentage could only be arrived at properly by a method of trial and error, but for practical purposes it is sufficient to take a percentage which the designer finds most economical. In Fig. 15 we have taken 23 % of the ampere-turns per pole as expended on the iron. Thus the maximum ordinate of the field curve is only 77 % of the magnetomotive force

curve. At lower flux densities the saturation is not so great and the field-form follows more nearly the curve of ampere-turns, as shown by the thick line.

To plot this field-form more accurately it is necessary to work with an air-gap-and-tooth saturation curve as shown on page 78.

If we take the distribution of the magnetizing current after it has advanced 30° in phase, the ampere-wires in the various slots will be 0·86, 0·86, 0·86, etc., respectively, as given in the second line along the top of Fig. 15, and magnetomotive

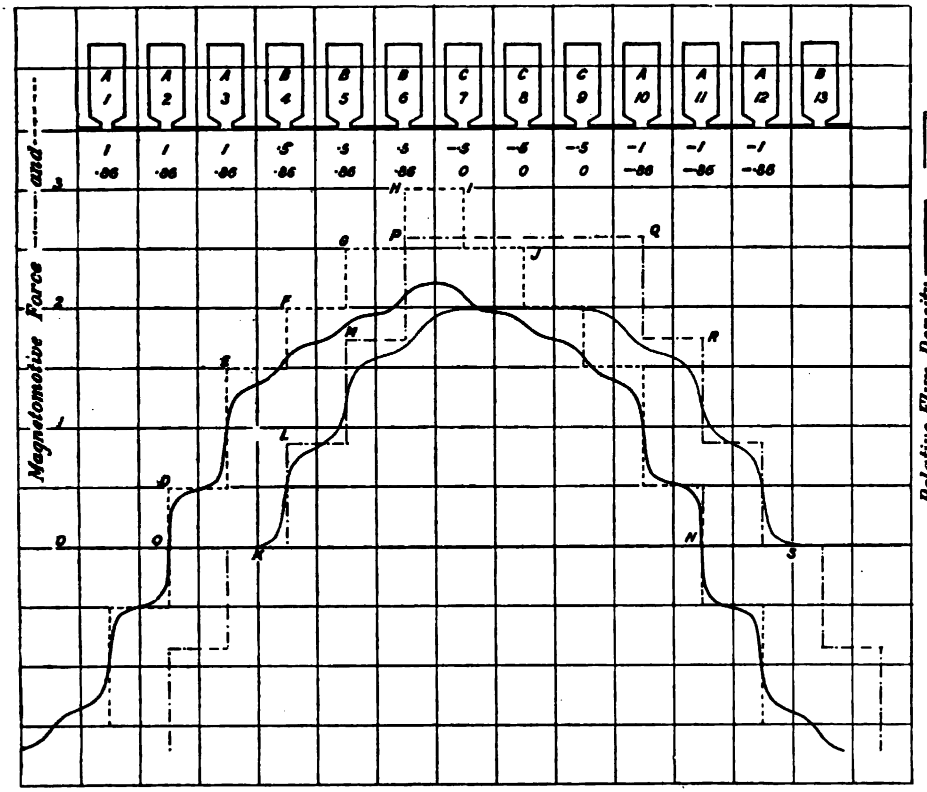

FIG. 15.—Field-form of induction motor for two different shapes of magnetomotive-force wave-form.

force curve will be of the form shown by the chain dotted curve *KLMPQRS*. The field-form will then be slightly different in shape, as shown by the thin full line.

If we run a planimeter around the flux curve shown by the thick line, and then around the rectangle whose height is equal to the maximum height of this curve and whose base is given by the pole pitch, we will find that the ratio between the readings is 0·68. This is therefore the value of K_f at the instant when the magnetomotive force is as shown by the dotted line *ODEFGHIJN*. If we go through the same process for the thin-line curve, we will find that the value of K_f comes out 0·695 for the instant when the magnetomotive force has a distribution as shown by the line *KLMPQRS*. The area of the thin curve is 5 % less than

the area of the thick curve, so that the highest ordinate in the thick curve is proportional to the maximum B, and the constant 0·68 used in connection with this flux-density will give us the maximum flux per pole.

The field-form of any machine can be worked out in the same way as shown in these examples.

In this chapter and the next we give some simple graphical methods of laying out the field-form and the E.M.F. wave-form, and for calculating the value of K_e. It may be well here, and in some subsequent notes, to give the analytical methods by which the wave-form of the E.M.F. can be calculated. As a first step, it is necessary to express the distribution of B in the air-gap by means of Fourier's series. For a symmetrical curve on no-load this will be:

$$B_x = B_1 \sin \theta_x + B_3 \sin 3\theta_x + B_5 \sin 5\theta_x + \text{etc.}, \quad \text{(1)}$$

where B_x is the flux-density at a point x on the periphery of the armature, and θ_x is the angle on a two-pole machine which x has passed through, measured from the neutral plane (see Fig. 321, p. 305).

Where the field-form is a simple rectangle * (see Fig. 322), we have

$$B_x = \frac{4}{\pi} B_g \left(\cos a \sin \theta_x + \frac{1}{3} \cos 3a \sin 3\theta + \frac{1}{5} \cos 3a \sin 5\theta_x + \dots \right). \quad \text{(2)}$$

Where the field-form is a trapezium (see Fig. 323), we have *

$$B_x = \frac{4}{\pi} \frac{B_g}{a} \left(\sin a \sin \theta_x + \frac{1}{9} \sin 3a \sin 3\theta_x + \text{etc.} \right), \quad \text{(3)}$$

and writing $\beta = \frac{2a}{\tau}$,

$$B_x = \frac{8}{\pi^2} \frac{B_g}{\beta} \left(\sin \beta \frac{\pi}{2} \sin \theta_x + \frac{1}{9} \sin 3\beta \frac{\pi}{2} \sin 3\theta_x + \dots \right). \quad \text{(4)}$$

Where the field-form is not of any simple shape, these coefficients, B_1, B_2, B_3, etc., can be determined by any of the methods of harmonic analysis.†

The wave-form of the E.M.F. generated in a band of conductors, moving with a velocity v at right angles to the direction of B, will depend upon the width of the phase-band of conductors and their arrangement in slots. When the field-form does not follow a simple sine law, the placing of the conductors in slots may give rise to ripples in the wave-form of the E.M.F., and calls for very careful analytical investigation if a more exact wave-form is to be ascertained (see pp. 304 and 313). The effect of these ripples in changing the virtual value of the E.M.F. generated in three-phase generators is, in practice, usually very small. Even where a ripple is very noticeable on an oscillogram, its effect on the virtual value of the voltage will be small, because the vector representing its maximum value must be added at right angles to the fundamental vector (see p. 33). We are therefore justified, in the graphical method given in the next chapter, in neglecting the effect of the high-frequency ripples in calculating K_e.

* Dr. S. P. Smith, "The Non-salient Pole Turbo Alternator and its Characteristic," *Jour. Inst. Elec. Engineers*, vol. 47, p. 562. See also paper by Smith and Boulding, *ibid.* Jan. 1915.

† Fischer-Hinnen, *Elektrotech. Zeit.*, xxii. p. 422, 1901; *Elektrot. u. Maschinenbau*, xxvii. p. 335; and see Silvanus P. Thompson, *Proc. Phys. Soc.*, xix. p. 443 (1905), *Electrician*, lv. p. 78, and *Proc. Phys. Soc.*, Aug. 1911, p. 334; R. Beattie, *Electrician*, lxvii. pp. 326, 370, 444 (1911). See footnote *ibid.*, p. 326, for list of references to literature on the subject.

CHAPTER III.

THE MAGNETIC CIRCUIT (*continued*).

THE ELECTROMOTIVE FORCE COEFFICIENT K_e.

WE will now proceed to give the methods of determining the constant K_e, by which the electromotive force of any machine is calculated when using the formula,

$$E = K_e \times \text{revs. per sec.} \times \text{conductors in series} \times A_g \mathrm{B} \times 10^{-8}.$$

The calculation of the coefficient K_e. In commutating machines of the ordinary type, in which the pitch of the armature coils is approximately the same as the pitch of the poles, the coefficient K_e is the same as the coefficient K_f. The reason is that the electromotive force generated in the conductors of a machine of this kind is proportional to the average value of the flux-density in the gap. In a continuous-current machine having a field-form like that given in Fig. 13 with a coefficient, $K_f = 0{\cdot}75$, the electromotive force generated in all the conductors in series between two brushes is 0·75 of what it would be if the flux-density were uniform all along the gap and of the same value as the maximum flux-density in Fig. 13.

Therefore, in a continuous-current machine or rotary converter, where we are given the constant K_f for the field-form, we have at once the constant K_e for finding the electromotive force.

EXAMPLE 1. The diameter of the armature of a certain frame is 25″ and its length 11″, so that the area of the active surface $A''_g = \pi D l = 865$ sq. in. Assume that the flux-density in the gap is 60,000 lines per sq. in. Then the magnetic loading, $A''_g \mathrm{B}''$ is $5{\cdot}2 \times 10^7$. How many conductors must we have in series on the armature, to generate 500 volts, when the machine is running at 900 revs. per min.?

First find the field-form constant K_f. Let this be 0·7, then $K_e = 0{\cdot}7$. From page 6 we have

$$500 = 0{\cdot}7 \times \tfrac{900}{60} \times 5{\cdot}2 \times 10^7 \times 10^{-8} \times Z_s,$$

$$Z_s = 92.$$

If 92 is not a very convenient number of conductors to get into the armature slots, we might choose the number 96, and make 12 slots per pole with 8 conductors per slot. We would then check over our calculations again as below,

$$500 = 0{\cdot}7 \times \tfrac{900}{60} \times A''_g \mathrm{B}'' \times 10^{-8} \times 96.$$

This gives us $$A''_g \mathrm{B}'' = 4{\cdot}96 \times 10^7.$$

EXAMPLE 2. Suppose that we wish to build a rotary converter running at 500 R.P.M. to generate 560 volts on a 6-pole frame, having an armature diameter of 36". We wish to have 54 commutator bars per pole, giving 108 conductors per pole in a lap winding. What will be the length of the armature if the flux-density is not to exceed 10 kapp lines per square inch in the air-gap? Having found (see page 14) the field-form constant $K_f = 0{\cdot}73$, write

$$560 = 0{\cdot}73 \times 500 \times 108 \times A''_g \mathsf{B}_K \times 10^{-6},$$

$$A''_g \mathsf{B}_K = 14200.$$

If $\mathsf{B}_K = 10$, $A = 1420 = \pi \times d \times l$.

Now $\pi d = 113$; $\therefore l = 12{\cdot}5$.

EXAMPLE 3. A small motor is running on a 250 volt circuit. Diameter of armature = 28 cms., length 14 cms., speed 1000 R.P.M. Total conductors, 384 in two-circuit winding, giving 192 in series. What is the flux-density in the gap? Allow 10 volts drop in armature and brushes, giving the back E.M.F. = 240. Let $K_f = 0{\cdot}72$.

$$240 \times 10^8 = K_f \times R_{pm} \times \mathsf{B} \times A_g \times Z_s \times \tfrac{1}{60} \text{ (see page 16).}$$

Now $A_g = \pi \times 28 \times 14 = 1230$;

$$\therefore 240 \times 10^8 = 0{\cdot}72 \times 16{\cdot}6 \times \mathsf{B} \times 1230 \times 192,$$

$$\mathsf{B} = 8500.$$

The calculation of K_e for an alternating-current machine. In alternating-current machines it is convenient to arrange the coefficient K_e so that it makes provision for the fact that the voltage measured at the terminals is the square-root of the mean square voltage, and also for the particular arrangement of winding whether two-phase or three-phase.

In actual practice K_e has usually been determined once and for all for the type of field-magnet employed, and it is very seldom that the designer of a machine has to go through the process of determining it. Nevertheless it is well for him to always bear in mind the factors upon which K_e depends.

Let us take an ordinary three-phase star-wound generator, and calculate K_e for a given field-form. The voltage measured at the terminals of a three-phase star-wound generator is the resultant of the electromotive force generated in all the conductors in two legs of the star. These conductors are distributed over an arc of 120 degrees, and there is therefore a wide difference of phase between the electromotive forces generated in the first and the last conductors of the phase band. Our method of calculating K_e must therefore take into account these differences of phase as well as any peculiarities of the field-form, and also the fact that only two-thirds of the whole armature conductors are in series between the terminals.

It is convenient to take the symbol Z_a in the formula for a three-phase machine to represent the total number of conductors on the armature (except of course where there are several paths in parallel, in which case Z_a would be equal to $Z_T \div c$, where Z_T is the total number of conductors and c is the number of paths in parallel).

We will therefore give K_e such a value that the virtual volts

$$E = K_e \times \text{revs. per sec.} \times Z_a \times A_g \mathsf{B} \times 10^{-8}. \qquad (1)$$

Or in kapp units,

$$E = K_e \times \text{R.P.M.} \times Z_a \times A''_g \mathsf{B}_K \times 10^{-6}. \qquad (2)$$

Or in C.G.S. lines per sq. inch,

$$E = K_e \times \text{revs. per sec.} \times Z_a \times A''_g \mathrm{B}'' \times 10^{-8}.$$

In all the formulae K_e is the same. A_g is the area of the active face of the armature in sq. cms. $= \pi Dl$. A''_g is the area of the active face in sq. inches.

The value of K_e depends not only upon the field-form, but also upon the arrangement of the armature conductors. The simplest three-phase case to take is where the field-form is sinusoidal and the armature conductors are distributed uniformly, each phase occupying exactly 60° of arc, the phases being connected in star in the usual manner (see Fig. 116, page 97). We then have $\frac{2}{3}$ of the conductors in series between any two terminals. The breadth coefficient is the ratio of the chord of 120° to its arc or $1{\cdot}73 \div \frac{2\pi}{3} = 0{\cdot}828$. We can therefore in this simple case calculate the value of K_e directly.

We have
$$K_e = \tfrac{2}{3} \times 0{\cdot}828 \times 0{\cdot}707 = 0{\cdot}39.$$

Similarly for the simple two-phase case with sinusoidal field-form,

$$K_e = \tfrac{1}{2} \times 0{\cdot}9 \times 0{\cdot}707 = 0{\cdot}317.$$

It is well to remember these two numbers, as they give us an easy check on calculations of K_e when the field is not sinusoidal. For instance, in an ordinary three-phase case, where the field-form is rather broader than the sine wave, we expect to get K_e rather more than 0·39, and where the field-form is more slender K_e will be less than 0·39.

We will work out the constant K_e for a given flux distribution in a simple star-connected armature. In this case Z_a stands for all the conductors on the armature, unless there are two or more conductors in parallel per phase. If there were c paths in parallel per phase, we should have to divide the total conductors Z_T by c to get Z_a. Similarly in three-phase mesh-connected armatures and in two-phase armatures, unless there are several paths in parallel per phase, Z_a represents all the conductors on the armature.

These symbols we will use throughout the book.

We will later give some curves from which we can read off the values of K_e for different shapes of pole, but it is well to see how K_e is calculated in any particular case.

Take for example a three-phase alternator having a pole with the bevel shown in Fig. 16, the pitch of the pole is 14 in. the width 8 in. The air-gap is 0·4 in. and there is a one-inch bevel on the corner of the pole, increasing the air-gap from 0·4 to 0·6 in.

First plot the field-form in the manner shown in Fig. 10. We will assume that there are 12 conductors per pole, that is 4 conductors per phase per pole. These conductors are shown by the little circles. Write down the values of the ordinates of the flux curve at points over the equally spaced conductors, as shown in the figure. It is best to place the conductors relatively to the pole so that these ordinates may fairly represent the average flux immediately adjacent to the conductor. It will be seen in Fig. 16 that if we take the ordinates 0, 9, 28, 80, 100, 100, 100, 100, 100, 80, 28 9, 0,

they represent sufficiently well the distribution of the flux. Here we have taken an arbitrary value of 100 for the maximum ordinate of the field.

Now, we know that in a star-connected armature the voltage between the terminals A and C is generated in the conductors of the phases A and $-C$ in series with one another. Consequently, when the conductors are in the position shown in Fig. 16, the instantaneous value of the E.M.F. generated in A and C will be proportional to the sum of all the eight ordinates 0, 9, 28, 80, 100, 100, 100, 100.

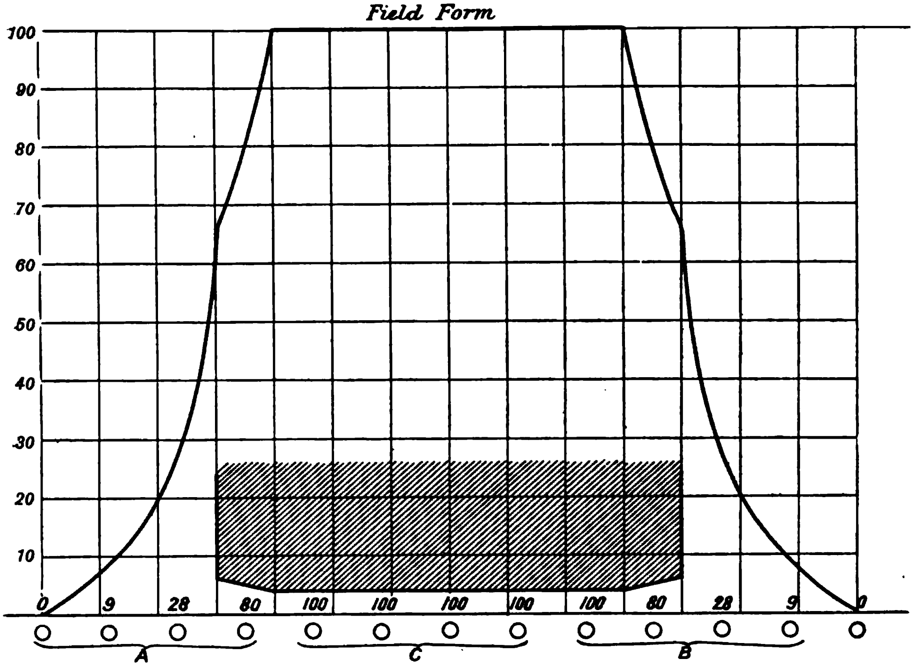

FIG. 16.—Field-form of three-phase generator, showing relative values of flux-density opposite the conductors of the three phases.

When the pole has moved to the right over the pitch of one conductor, the instantaneous E.M.F. will be proportional to the sum of 9, 28, 80, 100, 100, 100, 100 and 100, and so on. Consequently we may find values which are proportional to the instantaneous values* of the E.M.F. at various instants throughout the cycle, by the process worked out below. The process is as follows: From the sum of eight ordinates subtract the ordinate on the left and add a new ordinate on the right.

* The method g ven here does not enable one to plot the exact wave-form of the electromotive force as it would appear on an oscillograph. Where a slotted armature is employed there is a continual change in the field-form as the pole moves in the vicinity of the armature teeth, giving rise to high-frequency electromotive forces, which are superimposed as ripples upon the main wave-form of the electromotive force (see p. 309). In modern mechanics these ripples are avoided as far as possible by bevelling off the corners of the pole, by making the slots semi-closed and by employing a sufficient number (not less than six) of slots per pole. Where proper precautions of this kind are taken the ripples are of little consequence, and the disturbances in the field-form do not affect the virtual value of the generated electromotive force.

The values which are obtained after each operation are distinguished by being enclosed between heavy lines.

		Squared ordinates.
	0	
	9	
	28	
	80	
	100	
	100	
	100	
	100	
	$\boxed{517}$	267,000
subtract	0	
	517	
add	100	
	$\boxed{617}$	380,000
subtract	9	
	608	
add	80	
	$\boxed{688}$	474,000
subtract	28	
	660	
add	28	
	$\boxed{688}$	474,000
subtract	80	
	608	
add	9	
	$\boxed{617}$	380,000
	100	
	517	
	0	
	$\boxed{517}$	267,000
	100	
	417	
	-9	
	$\boxed{408}$	167,000

	Squared ordinates.
$\boxed{408}$	
100	
308	
-28	
$\boxed{280}$	78,500
100	
180	
-80	
$\boxed{100}$	10,000
100	
0	
-100	
$\boxed{-100}$	10,000
80	
-180	
-100	
$\boxed{-280}$	78,500
28	
-308	
-100	
$\boxed{-408}$	167,000
9	
-417	
-100	
$\boxed{-517}$	267,000

These values repeat themselves through successive cycles, and the best check on the arithmetical process we have gone through is to see whether we have come back to the same value for the sum when we have come back to the same relative position of conductors and pole. Thus, in the example given, we start with 517, and after twelve operations we get back to 517, but the value is now negative, because we are under the pole of opposite polarity. If we now plot the values we obtain a curve like the thick curve given in Fig. 17. This gives us the E.M.F. wave-form of the alternator. It is best to begin this plot where the values change from negative to positive, because between the positive and negative value there will be a point where the E.M.F. is zero. It will be noted that though the field-form is often angular and very far removed from a sine wave, the E.M.F. wave-form has its corners more rounded off, because it is really the summation of eight field forms, each displaced by one-twelfth of the pole pitch (see pp. 33 and 304).

The next step is to find the square root of the mean square value of the E.M.F. wave-form. For this purpose square each ordinate, and plot again as shown in

the thin curve in Fig. 17. If we were to run a planimeter around the curve thus obtained, and divide the area by the length of the base, we would obtain the mean value of the square, and the square root of this would give us the square root of mean square. But it is not necessary when finding K_e to trouble about the length of the base line. We argue in this way. If all the twelve conductors were connected in series, and if the field-form were a rectangle of height 100, extending along the whole pitch of the pole, then the maximum E.M.F. generated would be 1200 as against 700, as given in Fig. 17. Moreover, the E.M.F. would remain at 1200 all the time. The square of 1200 is 1,440,000. This taken as an ordinate in Fig. 17 would be off the paper, but we can plot it to one-tenth scale, as shown by the dotted rectangle.

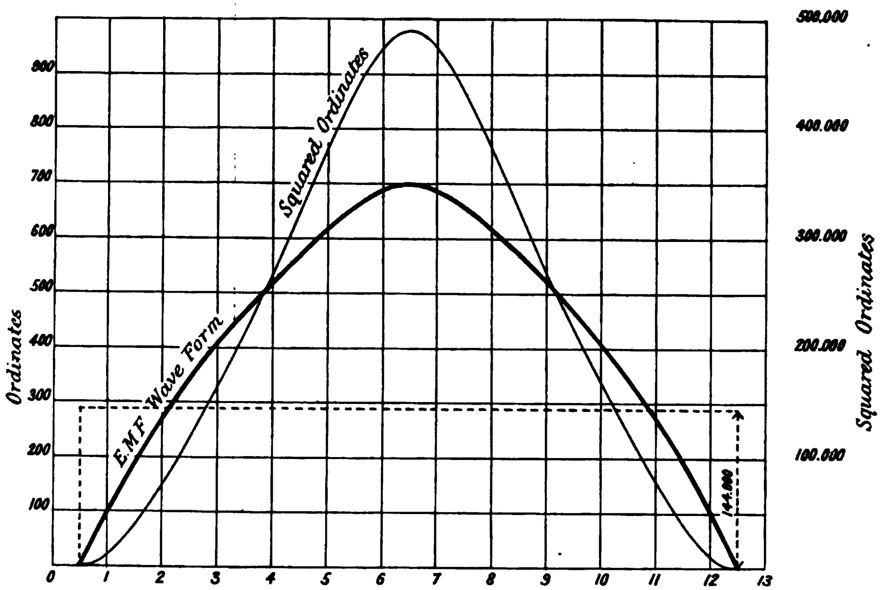

FIG. 17.—E.M.F. wave-form plotted from the summed ordinates of Fig. 16. Also the curve of squared ordinates.

Run a planimeter around the curve of the squared ordinates. Say that this gives us the reading 2116. The square root of this is 46. Now run the planimeter around the dotted rectangle. Say the reading is 1346. We must multiply this by 10, because we plotted to one-tenth scale. This gives us 13,460. The square root is 116. Therefore the square root of mean square value of the E.M.F. generated in twelve conductors by the full rectangular field-form, being taken at 116, the square root of mean square value of the E.M.F. generated in eight conductors by the field-form shown in Fig. 16 is 46, and the ratio of 46 : 116 is 0·396. This gives us K_e for a three-phase star-wound armature having a pole of the dimensions given in Fig. 16.

To sum up, the process of finding K_e for a simple three-phase winding and for any given field-form is as follows: Write down the values of twelve equidistant ordinates which fairly represent the field-form, the maximum being taken at 100.

Take the sum of the first eight of these and go through the process shown on page 27. Subtracting an ordinate from one end of the line, and adding the next ordinate and so on, obtain twelve summation values. These if plotted would give the E.M.F. wave-form. Square each of these values, and plot to any convenient scale. Run a planimeter around the curve and take the square root of the reading. On the same base line draw the rectangle with ordinate 1,440,000 plotted to one-tenth scale. Run a planimeter around this rectangle, multiply the reading by 10 and take the square root. The ratio of the roots is K_e.

If we go through this operation with the field-form given in Fig. 14, we obtain the value 0·4 for K_e. The same method would be employed for a two-phase machine, but in this case we would take the summation of six ordinates instead of eight. We will find that K_e for an ordinary two-phase generator with a field-form like Fig. 14 will come out 0·325. It will be found that the above method gives results sufficiently accurate, notwithstanding the fact that the number of slots per pole is different from twelve. Where, however, there is only one slot per phase per pole (a very rare case in modern machines), K_e would be multiplied by 1·045 for a star-connected armature and 1·15 for a mesh-connected three-phase armature. Sometimes the arrangement of the conductors in the phases is not as simple as shown in Fig. 16. There may be a short chord-winding as shown in Fig. 126, or a single-phase machine may be wound with a band of conductors covering an arc greater or less than two-thirds of the pole pitch. Whatever the arrangement of the conductors may be, we can calculate the value of K_e by laying out the conductors and the field-form as shown in Fig. 16, and after observing which of the conductors are in series with one another, and whether the ordinates of the flux help or oppose one another in generating the E.M.F., making a summation at a number of convenient relative positions of armature and field. It will be found that it is convenient to take the ratio of the square root of mean square voltage actually generated in the conductors in series to the square root of mean square voltage which would be generated in all the conductors arranged with a full pitch winding moving in a uniform flux-density as great as the maximum flux-density in the gap. The advantage of making our K_e a fraction of the hypothetical maximum effect is that we see at a glance how much we are losing or gaining by any arrangement of conductors, and we are less likely to make a mistake in the calculation when we obtain a number whose reasonableness we can at once estimate.

We have seen that in those cases where the flux distribution curve is of sine form, the value of K_e can be calculated at once without going through the process described in the preceding pages, and the figures, 0·39 for the three-phase case and 0·317 for the two-phase case, can be used as guides in guessing the value of K_e when we have not time to work it out.

In actual practice it is not necessary to go through the calculation like that given above, except in special cases where the field-form is of a new shape. The constant K_e is known for the frame and for the type of winding we intend to employ. For common shapes of salient poles having the pole arc equal to 0·675 of the pole pitch and having the corners bevelled as shown in Fig. 16, the constant K_e for a three-phase full-pitch star winding is 0·4, and this constant can generally be used in rough calculations of all similar machines. The effect of deepening the

bevel or of reducing the width of the pole is to reduce K_e. The effect of reducing the bevel or of widening the pole is to increase K_e. We may take as a good standard bevel one which is $\frac{1}{8}$ the width of the pole, and then work out the

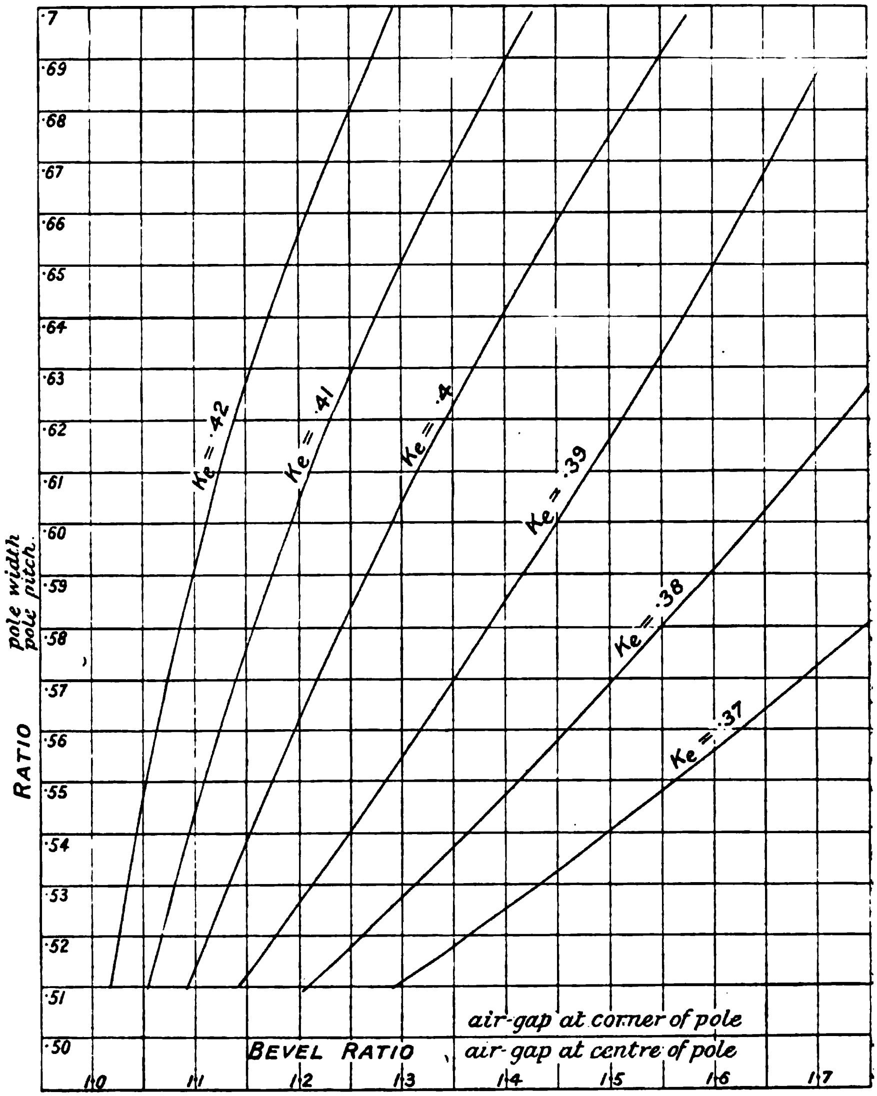

FIG. 18.—Values of K_e for different values of the ratio $\frac{\text{pole width}}{\text{pole pitch}}$ and different values of the bevel ratio, the value of c/g being 5. (See Fig. 10.)

values of the constant K_e for different depths of the bevel and different widths of the pole. This has been done for a three-phase machine with a full-pitch star-connected winding, and the results plotted in Fig. 18. From this figure the values of K_e for various shapes of pole can be read off directly. The values

given in the figure cover all the cases commonly met with in practice. For any case which is not directly covered it is easy to find a field-form falling under the variables provided for under the figure, which has the same general shape and the same area as the field-form of the case in question and whose shape is so nearly the same that K_e will practically have the same value. The effect of chording the winding is fully considered on page 113.

The effect of saturating the armature teeth is to make the field-form wider for a given maximum flux-density in the gap, and this will affect the value of K_e. The field-form is easily plotted by the methods described on pages 18 and 395. We can then either square the ordinates and obtain the constant K_e as described on page 27, or we can choose a field-form falling under Fig. 18 that has the same general outline and the same area, and read off K_e with sufficient accuracy for all practical purposes. The method of finding K_e for an induction motor is the same. The shape of the field-form will depend on the amount of saturation, and if accuracy is required the number of ampere-turns required for the teeth would be worked out by the method considered on page 78. In general, the K_e for a full pitch winding and with 25 % of the magnetizing ampere-turns thrown on the teeth may be taken at 0·415. We will work out below the K_e for the induction motor whose field-form is plotted in Fig. 15. From Fig. 15, by taking the means of the ordinates of the two field-forms, we can get 12 ordinates which are proportional to the following figures: 0, 128, 246, 325, 380, 405, 420, 405, 380, 325, 246, 128.

We can now go through the process described on page 27 with these ordinates of the field-form, and thus obtain the ordinates of the E.M.F. wave. To make the matter clear we give the figures below:

		Squared ordinates.
	0	
	128	
	246	
	325	
	380	
	405	
	420	
	405	
	$\boxed{2309}$	5,320,000
subtract	0	
	2309	
add	380	
	$\boxed{2689}$	7230
subtract	128	
	2561	
	325	
	$\boxed{2886}$	8350
subtract and add	246	
	$\boxed{2886}$	8350
subtract	325	
	2561	
add	128	
	$\boxed{2689}$	7230
subtract	380	
	2309	
add	0	
	$\boxed{2309}$	5320

		Squared ordinates.
	$\boxed{2309}$	
subtract	405	
	1904	
add	- 128	
	$\boxed{1776}$	3160
subtract	420	
	1356	
add	- 246	
	$\boxed{1110}$	1230
subtract	405	
	705	
add	- 325	
	$\boxed{380}$	145
subtract	380	
	0	
add	- 380	
	$\boxed{-380}$	145
subtract	325	
	- 705	
add	- 405	
	$\boxed{-1110}$	1230
subtract	246	
	- 1356	
add	- 420	
	$\boxed{-1776}$	3160

The wave-form of the E.M.F. is plotted in Fig. 20. We now square the ordinates of the E.M.F. wave-form and obtain the curve of squared ordinates. Whenever the numbers become unwieldy we can divide by 10 or 100, because the actual values are of no consequence. Now, if we had 12 conductors, each with an E.M.F. of 420, the highest ordinate of the flux curve, we should have a maximum of 5060 instead of 2900. Squaring the 506 and plotting to a scale to bring it on the paper, we get

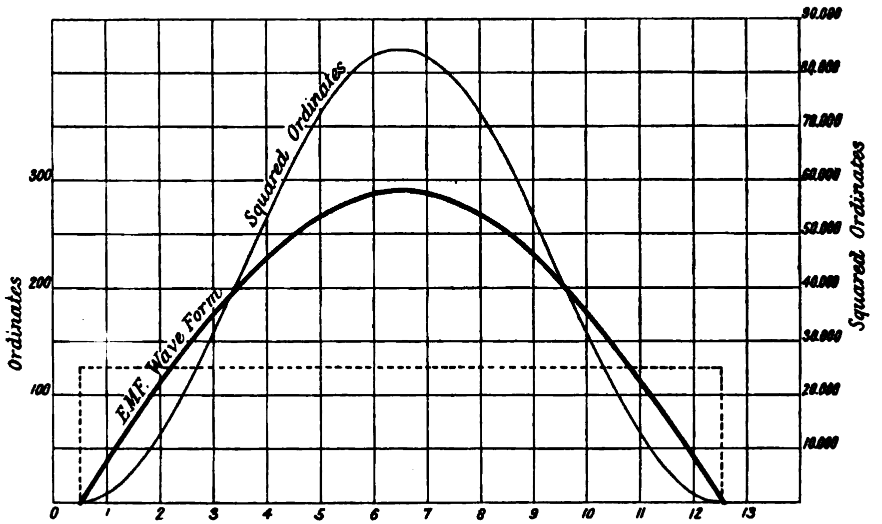

FIG. 20.—Wave-form of E.M.F. of induction motor having field-form as shown in Fig. 15, and the squared-ordinate curve of the same.

the dotted rectangle in Fig 20. We then run a planimeter around the squared ordinate curve and then around the dotted rectangle, and remembering that the rectangle is plotted to one-tenth scale, we find that the square root of the ratio between the areas is 0·415. This, then, is the coefficient K_e for the induction motor in question. If the saturation of the teeth had been higher, so as to give a field-form with a flatter top, the value of K_e would have been higher. Values of 0·42 and 0·43 are not uncommon.

In cases where the wave-form is required more accurately than when found by the above method, the designer may resort to the analytical methods which have been worked out in concise form in a paper recently by Dr. S. P. Smith and R. S. H. Boulding.* With the kind consent of these authors, an abstract of this paper is embodied here and on page 305.

It is shown on page 306 that where the flux is not pulsating the instantaneous voltage generated in a full-pitch of T_c turns is

$$e = 2T_c v l \mathrm{B}_x 10^{-8} \text{ volts},$$

where v is the velocity, l is the length of core, and B_x is the flux-density at the position x of the coil. Thus, the wave-form of the voltage in each conductor is the same as the wave-form of the flux curve.

When the conductors of the armature phase-band are uniformly distributed (see page 305) over an angle $2\sigma = \frac{S}{\tau}\pi$ (Fig. 321), extending at any instant between θ_1 and θ_2, the mean value of B throughout the coil span, 2σ, will be $\frac{1}{2\sigma}\int_{\theta_1}^{\theta_2} \mathrm{B}_x \, d\theta$. If now there are m coils,

* *Journ. I.E.E.*, vol. 53, page 205 (1915).

lettered $a, b, c, \ldots$ to m in the phase-band, and $mT_c = T$ total turns, the instantaneous voltage in T turns will be

$$\sum_a^m e = 2Tvl10^{-8}\frac{1}{2\sigma}\int_{\theta_1}^{\theta_2}\mathsf{B}_x\,d\theta.$$

From page 22 we have $\mathsf{B}_x = \mathsf{B}_1 \sin\theta_x + \mathsf{B}_3 \sin 3\theta_x + \mathsf{B}_5 \sin 5\theta_x,$

so that

$$\int_{\theta_1}^{\theta_2}\mathsf{B}_x\,d\theta = -\Big[\mathsf{B}_1\cos\theta_x + \frac{1}{3}\mathsf{B}_3\cos 3\theta \ldots \text{etc.}\Big]_{\theta_1}^{\theta_2}$$

$$= -\left\{\mathsf{B}_1(\cos\theta_2 - \cos\theta_1) + \frac{\mathsf{B}_3}{3}(\cos 3\theta_2 - \cos 3\theta_1) + \ldots\right\}$$

$$= -2\left\{\mathsf{B}_1\sin\frac{\theta_2+\theta_1}{2}\sin\frac{\theta_2-\theta_1}{2} + \frac{\mathsf{B}_3}{3}\sin 3\frac{\theta_2+\theta_1}{2}\sin 3\frac{\theta_2-\theta_1}{2} + \ldots\right\}.$$

Now $\frac{\theta_2+\theta_1}{2}$ is the angular position of the centre of the phase-band, and $\frac{\theta_2-\theta_1}{2}$ is equal to half the angle subtended by the phase-band or coil breadth. We have denoted the angle subtended by half the coil breadth by σ, so that

$$\sigma = \frac{\theta_2-\theta_1}{2} = \frac{S}{\tau}\frac{\pi}{2} \text{ (Fig. 321); and let } \frac{\theta_2+\theta_1}{2} = \theta.$$

Then

$$\sum_a^m e = 2Tvl10^{-8}\left\{\mathsf{B}_1\frac{\sin\sigma}{\sigma}\sin\theta + \mathsf{B}_3\frac{\sin 3\sigma}{3\sigma}\sin 3\theta + \text{etc.}\right\}$$

$$= 2Tvl10^{-8}\{\mathsf{B}_1 f_1'\sin\theta + \mathsf{B}_3 f_2'\sin 3\theta + \text{etc.}\}.$$

The coefficients such as $f_3' = \frac{\sin 3\sigma}{3\sigma}$ are the winding factors.

This expression shows us the effect of spreading the winding. If $\sigma = 0$, the wave-form of the E.M.F. is the same as for B_x, but as we widen the phase-band, making σ greater, the values of the winding factors become smaller, since $\sin h\sigma < h\sigma$, so that the higher harmonics are reduced, and the wave-form of the E.M.F. becomes more sinusoidal.

The values of the winding factors for different widths of phase-band are given in the table on page 307. Where the coefficients B_1, B_2, B_3, etc., are known, the wave-form generated in a uniformly distributed winding can be readily calculated in the manner indicated on page 308.

Where the conductors of the armature are not uniformly distributed (see page 305), but lie in slots (there being a whole number of slots per pole), the expression for the instantaneous value of the sum of all the E.M.F.'s generated in the coils takes the form:

$$\Sigma_a^m e = 2Tvl10^{-8}\{\mathsf{B}_1 f_1 \sin\theta + \mathsf{B}_3 f_3 \sin 3\theta + \ldots + \mathsf{B}_h f_h \sin n\theta\}.$$

But now the expression for the winding factors f_1, f_3, etc., is changed. We now have

$$f_h = \frac{\sin hm\frac{\gamma}{2}}{m\sin h\frac{\gamma}{2}},$$

where γ is the angle subtended by one slot (see Fig. 324, page 310).

In this case f_h does not always decrease as the order of the harmonic h increases, but periodically rises to a maximum (numerically equal to f_1) whenever h passes a multiple of $2Q$, Q being the whole number of slots per pole. This gives rise to ripples on the wave-form of E.M.F. of the order $h = 2Q + 1$ and $2Q - 1$. This is explained further on page 310.

Now the virtual value of the electromotive force,

$$E = \frac{1}{\sqrt{2}}\sqrt{E_1^2 + E_3^2 + E_5^2 + \ldots},$$

where E_1, E_3, etc., are the amplitudes of the several harmonics of the wave-form.

In three-phase star-connected machines $E_3 = 0$ and $E_9 = 0$. Where the fifth and seventh harmonics are in evidence the graphical methods of determining K_e given in this chapter will take care of them with sufficient accuracy. The harmonics due to the teeth are usually of too high an order to have their effect accurately calculated by the graphical method, but their amplitude is usually less than 5 per cent. of E_1, and we can see from the above expression for E that the addition of a harmonic of 5 per cent. makes only a negligible addition to the virtual value of the electromotive force, and could not be read on a voltmeter. We therefore neglect the effects of high harmonics in determining the value of K_e.

CHAPTER IV.

THE MATERIALS OF THE MAGNETIC CIRCUIT.

IN this chapter and the next we shall deal with the magnetic properties of iron and steel, and treat of the various parts of the magnetic circuit.

Following the course proposed on page 8, we shall as far as possible employ the same methods in dealing with the magnetic circuits of all classes of machines whether they be A.C. or C.C. generators, synchronous or asynchronous motors. What we require are general rules for making calculations relating to the air-gap, the teeth and slots, the armature iron behind the slots, the pole limbs and the yoke.

The units employed. It is a little difficult to decide what units should be employed in a book of this kind. Many designers in England and America use inches for measuring the dimensions of their machines, and amongst these some will employ kapp lines and others will employ C.G.S. lines for measuring magnetic flux. Of these some will write "60,000 lines per square inch" and others write "60 kilolines per square inch." Some engineers, on the other hand, prefer to make all their calculations in centimetres (using of course C.G.S. magnetic units), and then where necessary to convert their centimetres into inches for the British workman.

If the inch be taken as the unit of length, there is a great deal to say for the kapp line as the unit of magnetic flux. The speed of machines is invariably given in revolutions per minute, so that the formula,

$$\text{volts} \times 10^6 = \text{revs. per min.} \times \text{conductors} \times \text{kapp lines} \times \text{volt constant},$$

is very convenient, and in practice, the number of kapp lines being 6000 times smaller than the number of C.G.S., is more convenient to write down and to speak about than the number of C.G.S. lines. Thus one speaks of 10 kapp lines in the gap and writes it down 10, instead of talking of 60,000 C.G.S. lines, which must be written down either as 60,000 or as 60 kilolines.

All the above methods are so widely employed that we decided in the first instance to illustrate the rules given in the book by working out one example in each of the following systems of units:

(1) Dimensions in centimetres, magnetic flux in C.G.S. units.
(2) Dimensions in inches, magnetic flux in kapp lines.
(3) Dimensions in inches, magnetic flux in C.G.S. units.

This, however, was found to involve a great deal of repetition, and we have therefore in the main employed the C.G.S. system of units, that being the system which will probably be most generally employed in the future.

It is of course assumed that the reader is familiar with all the units with which he is concerned in magnetic calculations, but we will give here for his convenience a short statement of the relations between some of them.

UNITS OF MAGNETIC FLUX.

The unit magnetic flux, one C.G.S. line, has been named in America the *maxwell.*

As one often deals in dynamos with many millions of lines, some engineers prefer to work in *Megalines*, taking 1,000,000 lines as their unit. Others take *Kilolines* as their unit, and others again the volt-line or 100,000,000 C.G.S. lines. The latter unit is very useful when speaking of the total flux of a frame. These larger units, it is true, avoid the writing down of so many ciphers, and are therefore useful in private calculation where the unit is familiar. In a book on the subject, if one uses these units it is always necessary to write the word mega or kilo in stating the units, so that much of the advantage is lost. In those calculations in this book in which we employ C.G.S. units, we will use the volt-line as the unit when dealing with the flux per pole or when speaking of the total flux of a certain frame. We have, then,

100,000,000 maxwells = 1 volt-line.
1,000,000 maxwells = 1 megaline.
1000 maxwells = 1 kiloline.

Dr. Gisbert Kapp in his early writings on the dynamo—writings with which so many living designers are familiar—introduced the kapp line, which is equal to 6000 C.G.S. lines. By its use we avoid the necessity of dividing the revolutions per minute of a machine by 60 to convert to revolutions per second, and we use the factor 10^{-6} instead of 10^{-8} in the well-known equation for the voltage generated in a moving conductor. The kapp line being 6000 times greater than the C.G.S. line, the number which expresses the quantity of flux per pole in kapp lines generally runs to only three or four figures. At the same time one digit is often sufficient to express the flux-density in the gap.

Units of flux-density. A flux-density of one C.G.S. unit or one maxwell per square centimetre has been named in America the *gauss.* In this book we shall always write the flux-density expressed in C.G.S. lines per sq. cm. as B. Where inch measurements of length are used it is convenient to write B'' for the flux-density expressed in C.G.S. lines per sq. inch. If we have occasion to employ kapp lines, we can write B_K for the kapp lines per sq. inch.

We then have the following relations between these units:

1 C.G.S. line per sq. cm. = 1 gauss = 6·45 lines per sq. in.

Or, as one is generally dealing with thousands of lines to the sq. cm., one gets a better idea of the relation by writing:

10,000 C.G.S. lines per sq. cm. = 64,500 lines per sq. inch.
10,000 C.G.S. lines per sq. cm. = 10·75 kapp lines per sq. in.
10,000 C.G.S. lines per sq. in. = 1550 lines per sq. cm.
10,000 C.G.S. lines per sq. in. = 1·66 kapp lines per sq. in.
10 kapp lines per sq. in. = 60,000 C.G.S. lines per sq. in.
10 kapp lines per sq. in. = 9310 C.G.S. lines per sq. cm.

Units of magnetomotive force. Most designers use the ampere-turn as their unit of magnetomotive force and plot their magnetization curves accordingly.

The C.G.S. unit is about 80 % of this, it being necessary to multiply the ampere-turns by $\frac{4\pi}{10}$ to convert into M, the magnetomotive force in C.G.S. units. We therefore have the following relations:

1 ampere-turn = 1·257 C.G.S. units of M.M.F.
1 C.G.S. unit = 0·795 ampere-turn.

1 ampere-turn per centimetre on a uniform endless helix gives us a field of intensity $H = 1·257$ inside the helix.

1 ampere-turn per inch on a uniform endless helix gives us $H = 0·495$.

If $H = 1$ inside the helix, the ampere-turns per inch = 2·02.

MAGNETIC PROPERTIES OF IRON AND STEEL.

The four chief materials with which the dynamo designer has to deal, in the magnetic circuit, are: Cast Iron, Cast Steel, Forged Iron and Steel, and Sheet Steel.

Cast iron. Cast iron is used for yokes and spiders on account of its cheapness, the ease with which it is cast into complicated shapes and the ease with which it is machined. Though of much poorer magnetic quality than steel, it sometimes pays to use a heavy section of it instead of a light section of steel. Sometimes it happens that in big frames a great depth of material would in any case be necessary in order to obtain sufficient mechanical stiffness and the magnetic qualities of cast iron are then sufficiently good. For this reason cast iron is used to a great extent in the yokes of large continuous-current machines. Very often in slow speed A.C. generators it is necessary to provide a certain amount of fly-wheel effect, and the fly-wheel effect can be obtained most economically by employing deep cast-iron rims on the field-magnet wheel. There being a great depth of material, the cast iron is magnetically sufficiently good for the purpose. It is only at the root of the poles that one feels the pinch, due to the poor magnetic quality of the iron. Even where the number of ampere-turns on the magnetic circuit is somewhat increased by the use of cast iron instead of cast steel, there may be cases where the saving effected in using the cheaper iron pays for the cost of extra copper. Curve 6, Fig. 22, shows the relation between B and H for a fairly good specimen of grey cast iron. Average cast iron, as commonly employed in dynamo frames, is not

quite as good as this, if we take into account the whole casting including the skin. One might take Curve 7 as an average curve; if, however, the castings are small and have been cooled quickly, the magnetic properties may be worse than

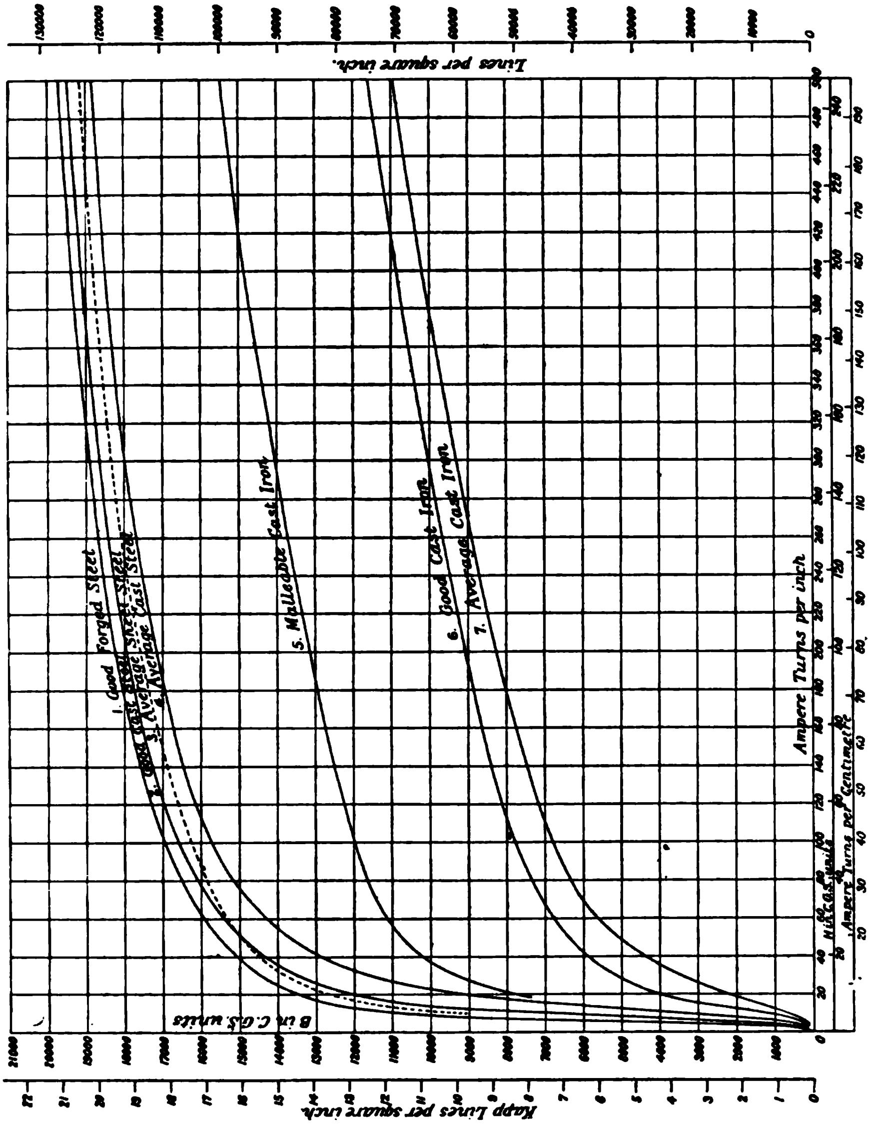

FIG. 21.—Magnetization curves of various qualities of iron and steel used in the manufacture of dynamos.

those shown on Curve 7. When cast iron cools, part of the carbon in it is deposited in graphitic flakes, while the remainder is combined with the iron and has the effect of greatly reducing its permeability. Very slow cooling results in a smaller percentage of combined carbon. It thus comes about that two different pourings from the same ladle may have considerably different magnetic properties,

according to the way in which the iron is cooled. Good average grey cast iron may have the following composition:

Graphitic carbon, - - - -	2·9	per cent.
Combined carbon, - - - -	0·3	„
Silicon, - - - - - - -	2·5	„
Sulphur, - - - - - - -	0·05	„
Phosphorus, - - - - -	0·14	„
Manganese, - - - - -	0·13	„

A sample of cast iron giving a curve as good as Curve 1 will have about 0·22 per cent. of combined carbon. The price (1914) of cast-iron dynamo frames in large quantities, delivered in a Midland town, is from 9s. to 13s. per cwt. for castings weighing between 1 and 20 cwt., depending on the difficulty of moulding, and from £8 to £11 per ton for castings weighing between 1 and 10 tons. The molten metal in the ladle may be taken at £6 per ton.

Malleable cast iron. When iron castings of no great thickness are heated to redness for several weeks in the presence of haematite or manganese dioxide, a considerable percentage of the carbon is burned out, and malleable iron is obtained, possessing somewhat better mechanical and magnetic properties than an ordinary cast iron. Curve 5 shows the magnetic properties of a sample of malleable cast iron. The quality of this material is, however, very uncertain, as much depends upon the proportion of the combined carbon which has been burned out. Malleable castings are conveniently used where it is necessary to have better mechanical qualities than are found in plain cast iron, and where the pieces are too small or too difficult to cast to justify the use of cast steel. They are sometimes used for the end plates of poles, and for the finger-plates of armatures.

A malleable casting having the permeability shown in Curve 5 might have the following composition:

Graphitic carbon, - - - -	2·0	per cent.
Combined carbon, - - - -	0·09	„
Silicon, - - - - - - -	1·1	„
Sulphur, - - - - - - -	0·01	„
Phosphorus, - - - - -	0·03	„
Manganese, - - - - -	0·08	„

The price of malleable castings (1914) depends largely upon the numbers ordered, but may be taken roughly at 29s. per cwt. for reasonable quantities of simple pieces weighing not less than 15 lbs. apiece. For smaller pieces the prices may be higher, and will depend on the difficulty of moulding.

Cast steel. From its chemical composition one would expect cast steel to possess very excellent magnetic qualities, and some samples of cast steel are as good, magnetically, at the point of saturation ordinarily employed in electrical machinery as forged steel; but, unfortunately, blow-holes and piping crevices sometimes occur in the castings, and accidents may happen in the cooling which bring about a rather poorer permeability. Curve 2, Fig. 22, shows the magnetic

qualities after annealing, of a fairly good sound casting of dynamo steel, having chemical composition as follows:

Combined carbon, - - - - -	0·2	per cent.
Silicon, - - - - - - -	0·15	„
Aluminium, - - - - - -	0·05	„
Phosphorus, - - - - - -	0·04	„
Sulphur, - - - - - - -	0·03	„
Manganese, - - - - - -	0·11	„

The quantities of all these impurities, except the carbon and manganese, might be doubled without appreciably altering the shape of the curve. An increase in the percentage of carbon reduces the permeability. If the specimen had not been annealed, the permeability at low inductions would have been lower, but the permeability at about $B = 18{,}000$ would have hardly been affected. The addition of manganese or chromium, or other hardening elements, reduces the permeability. An addition of nickel up to 4 per cent. has no deleterious effect. Some steel containing 2 per cent. of nickel shows a slightly higher induction at $H = 100$. Experience shows, however, that one cannot rely upon always getting as good material as is represented by Curve 2, and we may therefore take Curve 4 as the curve of an average specimen of a steel casting. In this curve we have allowed $3\frac{1}{2}$ per cent. of the space occupied, for blow-holes, and we have assumed that the annealing will not be quite as good as in Curve 2.

There are several great advantages to be gained in the use of cast steel in preference to cast iron. The permeability is so much higher that only one half of the cross-section of material need be employed (assuming always that we have sufficient mechanical stiffness), and the weight of the whole machine is greatly reduced.

When the pole limbs are made of forged or rolled steel a smaller section of limb can be employed where the yoke is of cast steel than if it is of cast iron, because there is not the same fear of excessive saturation at the root of the pole.

The cost of dynamo steel castings, delivered in a Midland town, is from 13s. to 15s. per cwt. for castings up to 10 cwt. depending on the difficulty of moulding and the numbers ordered, and from £11 to £13 per ton for heavy yokes of simple section.

It will be seen, in comparing these prices with the prices of cast iron, that if the weight of the steel frame can be reduced to one half of the weight of a cast-iron frame for the same machine, there is a considerable saving in the cost of material. It is, however, usual to allow for more metal being taken off the finished faces. Thus more cast steel goes to waste. The saving of freight on the completed machine must also be taken into account.

Another advantage in the use of cast steel for dynamo frames lies in the fact that the pole limb can in many cases be cast with the yoke, thus saving some cost in machining. With cast iron it would be false economy to cast the pole limb with the yoke, because the section of the pole limb if made of cast iron would have to be made very large, and this would call for an excessive weight of copper for

the winding. Steel castings have usually not as good a finish as iron castings, and it is difficult to make small sections and complicated shapes in cast steel. The cost of machining of steel yokes is greater than the cost of machining cast-iron yokes of the same size. Usually, with cast steel more chipping and preparation work is required. The cost of machining simple dynamo yokes of cast steel and cast iron is shown in Fig. 23. The curves have been plotted from the records of a large dynamo works where modern methods are employed. The figures for cost include the cost of chipping and preparing for the boring mill. A certain

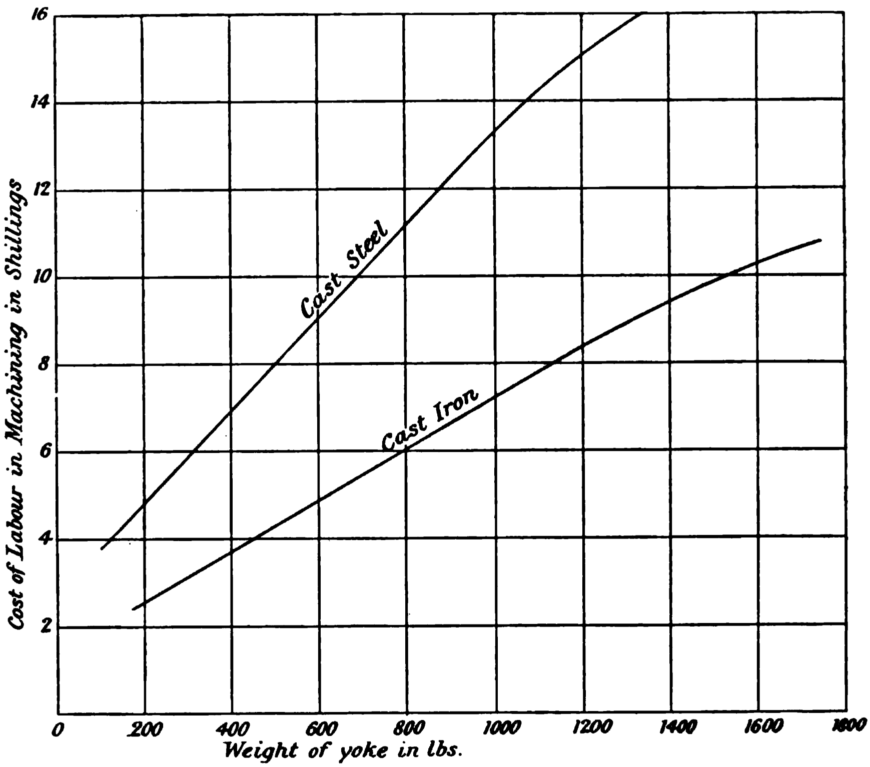

FIG. 22.—Cost of machining cast iron and cast steel yokes.

percentage (perhaps 30 or 40 per cent.) of the steel castings are defective and must have the flaws welded. The average cost of this may be taken at about 3s. 6d. per casting treated.

Another circumstance which must be taken into account in the choice of material is the shortness of time in obtaining delivery from the maker. Steel castings are only made by large steel manufacturers in certain centres, and it is sometimes difficult to obtain delivery, whereas there is very little difficulty in obtaining iron castings in any large manufacturing town, and most dynamo builders make their own.

Forged steel and iron. Low carbon steel, when forged so as to make it compact and homogeneous, is of all commercial materials the one to be relied

upon for its magnetic qualities; forged steel containing not more than 0·2 % of carbon is practically as good as pure iron for the magnetic parts of generators. Curve 7 gives the relation between B and H for a specimen of forged ingot iron made by the open-hearth process, whose chemical composition is as follows:

Combined carbon, - - - -	0·15 per cent.
Silicon, - - - - - -	0·06 „
Sulphur, - - - - - -	0·03 „
Phosphorus, - - - - -	0·04 „
Manganese, - - - - -	0·4 „

The specimen was annealed before testing. An unannealed specimen would have shown a lower permeability at $H=10$, but at $H=100$ B would have been practically as high as in Curve 1. The effect of adding carbon and other elements which have a hardening effect is the same in forged steel as with cast steel. This Curve 1 is about as good a magnetization curve as one can hope to get from any commercial material. It is probable that there is no material which is 4 % better at $H=100$. A very pure specimen of iron thoroughly annealed would show higher permeability at lower inductions, but the feature which helps the designer in increasing the output of a frame is the permeability at fairly high inductions. Some magnetization curves of steel that one sees have been plotted from measurements which do not sufficiently eliminate errors, and the figures obtained, particularly at high magnetizations, are often erroneous. The dynamo manufacturer, to be sure of his material, must test a specimen in an apparatus upon which he can make a direct comparison with materials whose qualities he knows to be good. Steel manufacturers' magnetization curves are useful as a guide, but unless we know the method of measurement, and the individual who made the test, too much reliance should not be placed upon them. In any case, one cannot be sure that the specimen faithfully represents the bulk. The only true test of the material of a dynamo yoke is a test of the finished machine.

The main objection to the use of forged steel or iron in the construction of dynamo frames is the cost of forging or machining the parts to the right shape. The material in the rough is very cheap—rolled bars of rectangular or round section can be bought at £9 per ton delivered in a Midland town. Whenever we can, without much labour, fashion parts of a dynamo, such as pole limbs, from the rough bars, no cheaper or better material can be used. One is sure that the material is solid, and one is fairly sure of the magnetic quality if the percentage of carbon is low. The mechanical qualities are also good. For rotating field-magnets which are to be subjected to very great centrifugal forces, it is possible to make a steel containing not more than 0·4 % of carbon and 3·5 % of nickel, having as good magnetic properties as are shown in Curve 2 in the higher reaches of that curve, and possessing the following mechanical qualities:

Ultimate tensile strength, - -	45 tons per sq. in.
Elastic limit, - - - - -	27 „
Extension of an 8 in. specimen, -	18 per cent.
Reduction in area, - - - -	40 „

FIG. 23.—Magnetization curve of dynamo sheet steel, giving the ampere-turns per centimetre up to very high values of B.

If, however, the percentage of carbon does not exceed 0·2 (the nickel being still 3·5 %), the ultimate tensile strength will be about 38 tons per sq. in. and the elastic limit 20 tons. The magnetization curve may then be as good as Curve 1 in the upper reaches. For low values of H the permeability will greatly depend upon the treatment which the material has had since the last annealing.

Sheet steel. The material from which dynamo sheet steel is rolled should be very low in carbon. The following is the analysis of a good specimen of ordinary dynamo steel:

Combined carbon,	0·09	per cent.
Silicon,	0·01	"
Sulphur,	0·042	"
Phosphorus,	0·089	"
Manganese,	0·36	"

The process of rolling it into sheets makes it if anything more compact and homogeneous than forged steel, but at the same time a thin layer of oxide is produced on the outside which, to a certain extent, reduces the permeability of an iron core built up of sheet metal. Care should be taken that this layer of oxide is not too thick. Sheet steel 0·06 in. thick, when reasonably clean and assembled under pressures such as are ordinarily employed in the building up of pole pieces, may be taken to be 95 % solid iron, the remaining 5 % is made up partly of oxide and partly of air spaces between roughnesses of the surface. The dotted curve 3 in Fig. 21 may be taken as giving the magnetic properties of good average dynamo sheet steel. An extension of this curve going up to very high flux-densities is given in Fig. 23. If sheet steel 0·02″ thick is papered with paper 0·0013″ thick, such as is used in the building of armature cores, the material can be compressed under the ordinary pressure used in dynamo construction, until it has the permeability of material 92 % solid. Where the sheet steel is only 0·016″ thick, and is papered with the same paper, one cannot rely upon the solidity being more than 89 %, unless the steel is particularly clean and the pressure to which it is subjected is very high. It is well for every manufacturer to make occasional tests of the solidity of his built-up punchings, so that proper allowance can be made for the space taken up by paper and air.

The cost of ordinary dynamo sheet steel may be taken at £10 or £11 per ton.

Alloyed steel. In recent years a steel alloyed with silicon has come largely into use for electrical machinery. The effect of adding between 1·8 and 5 per cent. of silicon to an almost pure iron is to greatly increase its electrical resistance and thus to reduce the loss in it due to eddy currents (see page 52). At the same time this addition of silicon has a marked effect on the permeability and on the hysteresis loss. The addition of silicon in a quantity less than 1·8 per cent. seems to slightly reduce the permeability of steel, but as the percentage is increased from 1·8 up to 4·8 the permeability for inductions below 13,000 or 14,000 is increased. This is shown in Fig. 24, which gives the magnetization curve * of two specimens of silicon steel and also for comparison the curves of three other steels,

*See Dr. S. Guggenheim, "The Magnetic Properties of Iron Alloys and their Uses in Alternate-current Design," *The Electrician*, vol. 64, p. 539.

one unannealed having comparatively poor permeability at low values of H, another annealed cast steel and the third an annealed specimen of the softest iron very low in carbon and silicon. It will be seen that though the specimens of silicon steel have as much as 0·2 per cent. of carbon they are very permeable at inductions below 13,000, and the greater the addition of silicon (below 5 %) the greater the permeability at low inductions. At inductions over 14,000 the addition of silicon lowers the permeability. This gives the magnetization curves of silicon steel a very decided shoulder.

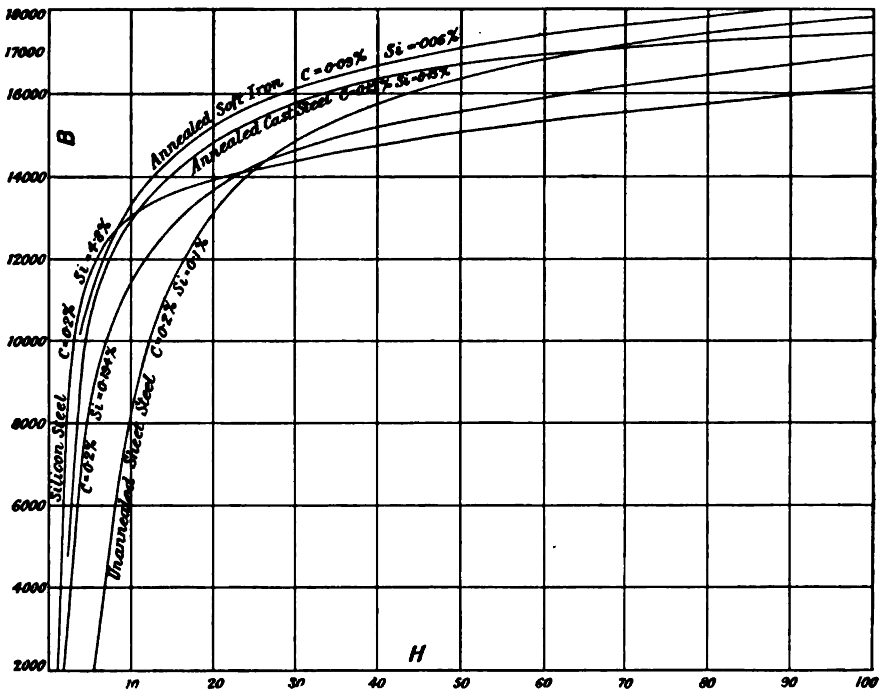

FIG. 24.—Showing the high permeability of silicon steel at flux-densities below 18,000 and the lower permeability at high flux-densities.

The effect of the addition of silicon on the hysteresis loss is shown in Table I. p. 48. The greatest loss at all inductions occurs in the steel alloyed with 0·18 per cent. of silicon. All these results were obtained from steels containing 0·2 per cent. of carbon. Many of the alloyed steels on the market are very low in carbon. A characteristic analysis of alloyed steel is as follows:

Carbon,	0·08	per cent.
Silicon,	3·0	,,
Sulphur,	0·03	,,
Phosphorus,	0·045	,,
Manganese,	0·2	,,

Although low in carbon, all these alloyed steels show a rather lower permeability than ordinary steel at high inductions. This is a feature to be taken into account when they are used in armatures with highly saturated teeth.

One drawback to the use of silicon steel for making stampings is its great hardness and brittleness. A steel containing as much as 4 % of silicon is very hard on the dies, and sometimes the sheet breaks up under the punch just as hard cast iron would. Even after the metal has been punched the teeth will sometimes break off. Steels with a lower percentage of silicon are made by some of the makers, which while preserving to a considerable extent the high resistance, and therefore the low eddy-current loss, are at the same time easy to punch and perfectly safe under bending stresses.

The hardening effect of the silicon, if it has not been carried too far, is of great service in the armatures of high-speed machines.

The cost of silicon steel 0·5 mm. thick, having a loss under standard conditions (see page 53) of 0·8 watt per lb., is from £20 to £23 per ton. For higher qualities with losses as low as 0·56 watt per lb. the price ranges up to £30 per ton.

LOSSES IN SHEET IRON.

The two losses occurring in iron subjected to an alternating-magnetic field are (1) the hysteresis loss and (2) the eddy-current loss. When considering the hysteresis loss a distinction must be drawn between an alternating field having a fixed orientation in the iron and a rotating magnetic field, in which the orientation of the induction rotates continuously. The difference in the hysteresis loss in these two cases has been investigated by Prof. F. G. Baily, and is clearly shown in Fig. 25 reproduced from his memoir.*

At low flux-densities and at flux-densities up to $B = 15{,}000$ the rotating field gives a rather greater loss than the alternating field, the general character of the upward sloping curves being the same, but after we reach the value $B = 16{,}000$ the losses produced by the rotating field decrease and come down almost to zero at $B = 20{,}000$. The losses produced by the alternating field go on increasing up to $B = 24{,}000$ after which they remain almost constant. The pure rotating field of constant strength seldom occurs in practice. It would occur in the rotor of a two-pole machine if it were not pierced by a shaft. In multipolar machines with annular cores the orientation of the flux-density rotates as the machine revolves, but the flux-density does not remain constant. The change that takes place may be regarded as a rotation of the flux with an alternating flux superimposed. The hysteresis loss, therefore, would be shown by a curve lying somewhere between the two curves in Fig. 25. The relative strengths of the rotating field and the alternating field differ at different depths in the core. In the teeth we have chiefly an alternating field. As it would take too much time in practical calculations to discriminate between the effects of the rotating and the alternating fluxes, it is usual to compile curves (based on the actual losses in machines) from which one can read off the number of watts per cubic cm. or per cubic in. for a given flux-density and given frequency. Such curves are given in Fig. 29.

* *Phil. Trans.*, 1896, vol. 187, A, pp. 715-746, "The Hysteresis of Iron and Steel in a Rotating Magnetic Field."

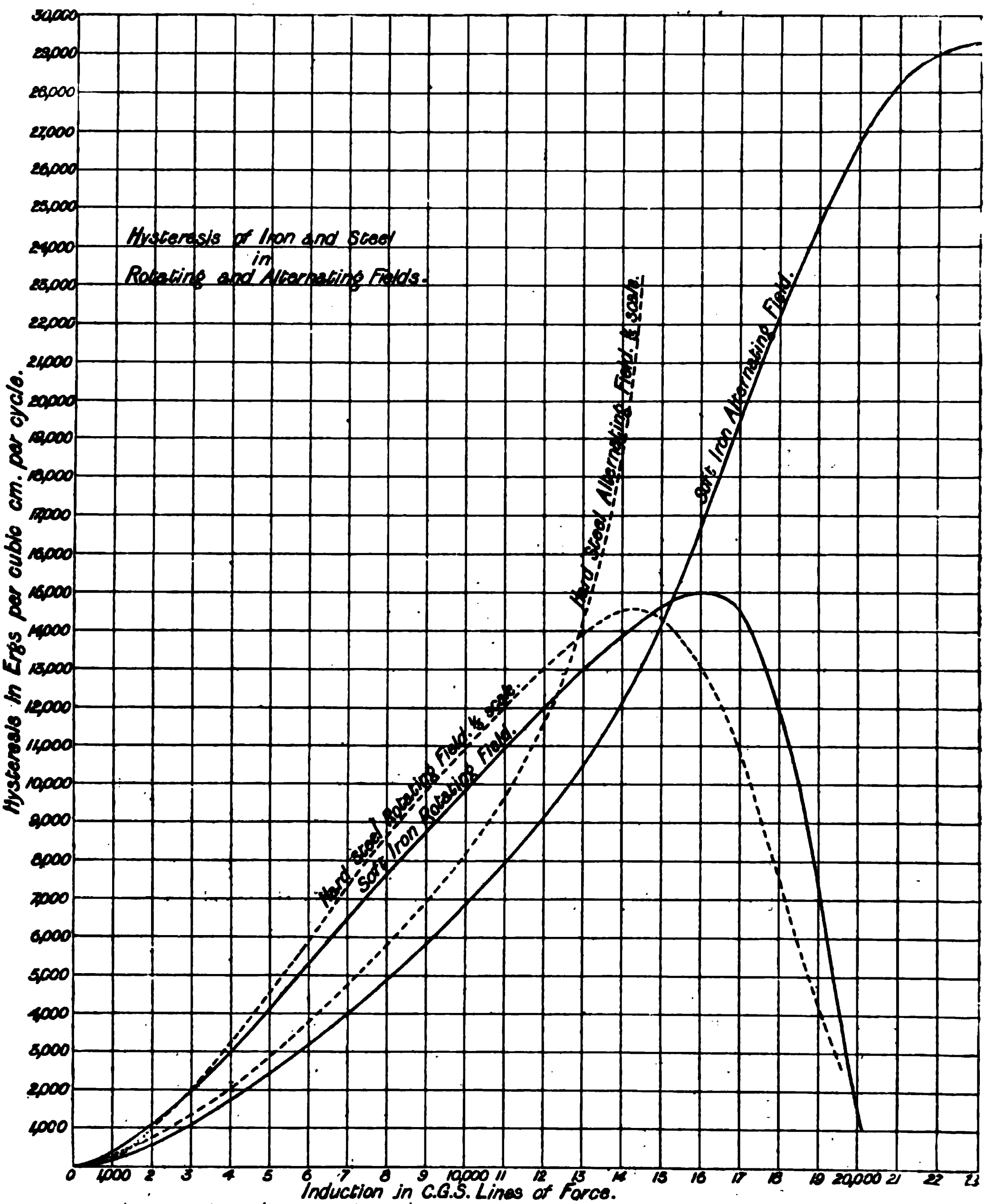

FIG. 25.—Hysteresis loss in soft iron and hard steel subjected to alternating and rotating fields.

For an alternating field the hysteresis loss follows the well-known law of Steinmetz, $W_h = \eta B^{1.6}$, sufficiently well for practical purposes up to flux-densities of 17,000 lines per sq. cm.

For higher flux-densities one must use a curve derived from experiment. Fig. 26, when used in conjunction with the hysteretic constant, is useful in

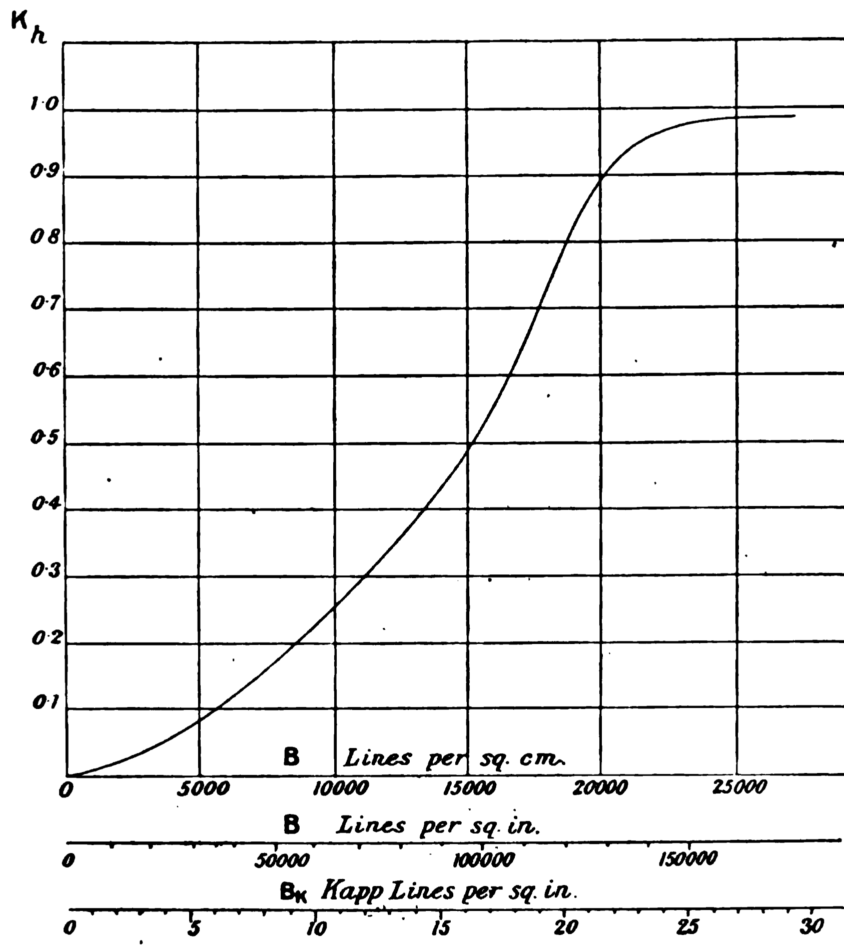

FIG. 26.—Showing how the hysteresis loss in iron increases with the flux-density.

giving the hysteresis loss up to any flux-density ordinarily employed in dynamos. In this figure K_h is a function of B, such that $K_h \times \eta$ = the hysteresis loss in joules per cycle.

Table I. (see p. 48) gives the value of the hysteretic constant for different kinds of iron and steel.

Fig. 26 has been arranged so that, whichever of the three commonly used systems of units is employed, the loss per cu. cm., the loss per cu. in. or the

loss per lb. can be readily arrived at. The following are the constants to be used in conjunction with K_h given in the figure:

$K_h \times \eta =$ joules per cu. cm. per cycle.
$K_h \times \eta \times n =$ watts per cu. cm. at frequency n.
$16{\cdot}4 \times K_h \times \eta \times n =$ watts per cu. in. at frequency n.
$59 \times K_h \times \eta \times n =$ watts per lb. at frequency n.

TABLE I. HYSTERETIC CONSTANTS.

Material.	Hysteretic constant $=\eta$.	Material.	Hysteretic constant $=\eta$.
Good dynamo sheet steel	0·002	Silicon steel (Si) = 1·8 %	0·004
Fair dynamo steel	0·003	The same steel (Si) = 0·2 %	0·0021
Silicon steel * (Si) = 4·8 %	0·00076	Very soft iron	0·002
,, ,, = 4 %	0·001	Cast iron	0·011 to 0·016
,, ,, = 3·5 %	0·0013	Cast steel	0·003 to 0·012
,, ,, = 3 %	0·0016	Hardened cast steel	0·028
,, ,, = 2·5 %	0·0022	Barrett's aluminium iron	0·00068

* See Dr. S. Guggenheim's paper referred to on page 43.

EXAMPLE 4. What is the loss due to hysteresis in the armature iron behind the slots of a 25-cycle generator, the maximum flux-density in the iron being 11,000 lines per square cm. and the volume of iron (which is of ordinary quality) being 250,000 cu. cm.? From Fig. 26, for $B = 11{,}000$ $K_h = 0{\cdot}29$. We will take the hysteretic constant as being 0·003.

$$0{\cdot}29 \times 0{\cdot}003 \times 25 \times 250{,}000 = 5400 \text{ watts.}$$

EXAMPLE 5. What is the hysteretic loss in the teeth of the same generator, the total volume of the teeth being 1500 cu. in. and the average flux-density in the teeth being 140,000 lines per square inch?

From Fig. 26, $K_h = 0{\cdot}955$,

$$16{\cdot}4 \times 0{\cdot}955 \times 0{\cdot}003 \times 25 \times 1500 = 176 \text{ watts.}$$

EXAMPLE 6. Suppose that we were prepared to work the iron behind the slots of this generator at 13½ kapp lines per sq. inch, how much extra loss would we have and how many lbs. of iron would we save?

250,000 cu. cm. of iron weigh 4320 lbs.,
11,000 lines per sq. cm. = 11·8 kapp lines per sq. in.,

$$\frac{4320}{1} \times \frac{11{\cdot}8}{13{\cdot}5} = 3770 \text{ lbs. giving a saving of 550 lbs.}$$

From Fig. 26, for $B_K = 13{\cdot}5$, $K_h = {\cdot}36$.

$$59 \times 0{\cdot}36 \times 0{\cdot}003 \times 25 \times 3770 = 6000 \text{ watts.}$$

$$6000 - 5400 = 600 \text{ watts extra loss at the higher flux-density.}$$

EDDY-CURRENT LOSSES.

If we had a simple alternating magnetic flux through the sheet steel of an armature, the direction of the flux being strictly parallel to the plane of the laminations, and if the individual sheets were perfectly insulated from one another, the eddy-current loss in watts per cubic centimetre of iron would be

$$W_e = \frac{\pi^2}{6} \times \frac{1}{\rho} \times t^2 \times n^2 \times B_{max}^2 \times 10^{-16}, \quad \text{.........................(1)}$$

where ρ is the specific resistance of the iron, t the thickness of the sheet in centimetres, n the frequency and B_{max} the maximum flux-density in lines per sq. cm.

In practice, however, these conditions are seldom met with. The flux in most dynamos partly alternates and partly rotates. The constant $\frac{\pi^2}{6}=1{\cdot}645$ should be increased considerably on account of this circumstance. Experiments upon perfectly laminated iron, subjected to a magnetic flux changing as it does in dynamos, indicate that the constant 2·8 is nearer the right value than 1·645. If we take the specific resistance of ordinary dynamo steel at the working temperature (50° C.) as $11{\cdot}7\times10^{-6}$, we get the formula for the eddy-current loss in watts per cu. cm. of iron,

$$W_e=2{\cdot}8\times\frac{1}{11{\cdot}7\times10^{-6}}\times t^2\times n^2\times B^2_{max}\times10^{-16}$$

$$=2{\cdot}4\times t^2\times n^2\times B^2_{max}\times10^{-11}. \quad \ldots\ldots\ldots\ldots(2)$$

We find, however, that the measured iron loss in a completed machine is usually much higher than the sum of the hysteresis and eddy-current losses calculated by the formulae given on pages 47 and 48. There are many reasons for this. The sheet iron is often bent about after the annealing in a way that increases the hysteresis loss. The insulation between the sheets is by no means perfect. There may be burrs on the edges which allow adjacent sheets to make metallic contact, or the filing of the slots produces a similar effect. It must be remembered that when the punchings are assembled in a cast-iron frame the edges of the punchings usually rest against the cast iron and make electrical contact with it. If, therefore, through the filing of the slots or from any other cause the punchings are in electrical contact on the working face of the armature, there is a complete electric circuit through which a current will pass driven by an electromotive force whose maximum value is equal to $2\pi nN\times10^{-8}$, where N is the total flux carried by the short-circuited punchings. Even if the punchings are insulated from the frame by some thin, hard insulating material (a plan which may be adopted with advantage when it is very important to keep down the iron loss), it is possible to have a circuit along the burred punchings in front of a north pole, with a return circuit along the burred punchings in front of a south pole. Or if the two sides of a tooth are burred over, a current will flow around the electric path thus formed, the electromotive force driving it being proportional to the flux threading through that part of the tooth.

Cast Iron

FIG. 27.—Eddy-current path.

Another cause of excessive iron loss is the passage of the flux along a path which is not everywhere parallel to the plane of the laminations. At the ends of the machine, part of the flux bulges out from the ends of the poles and enters the armature on the flanks, and there is thus a considerable component of the flux at right angles to the plane of lamination. This produces eddy currents both in the

end plates (whatever metal they are made of) and in the sheet iron. At the edges of every ventilating duct the same sort of action occurs on a small scale. Again, in armatures built up of segments there is always a little extra reluctance at those parts of the magnetic path where the breaks in the punchings occur, even though the punchings are arranged to break-joint. If from irregular machining of the frame the punchings are built up so as to make a closer joint at one end of the machine than at the other, as indicated in Fig. 28, the higher reluctance of the joint at one end causes the flux in a certain measure to crowd to the end where there is least reluctance. If now the bad joint is first at one end of the machine and then at the other, there is a tendency for the flux to take a wavy path, which necessarily has components at right angles to the plane of the laminations. Sometimes the punchings of the stator themselves build up so that the plane of lamination is itself wavy, and the flux in each section of the rotor as the machine rotates leaves and enters the wavy stator punchings along paths which have small components at right angles to the plane of those punchings, and therefore causes some extra eddy-current loss. Wherever a break or partial break occurs in the punchings, some of the flux is driven out into the surrounding frame, and causes a little loss. It is well known that there are certain relations between the number of breaks in the punchings and number of poles which cause this loss to be greater or less. If the number of poles is equal to number of breaks, the relation is good. If the number of poles and the number of breaks is such that there are at any instant as many north poles opposite breaks as there are south poles opposite breaks, the relation is good. If, however, the numbers are such that at one instant a great number of north poles are opposite breaks (the south poles being between breaks) and at another instant a great number of south poles are opposite breaks (the north poles being then between breaks), the relation is not so good. Thus we would not from choice have the number of poles $1\frac{1}{2}$ times the number of armature segments. This relation of the numbers is not impossible, but it is to be avoided if possible, particularly if the joints in the punchings are not well made.

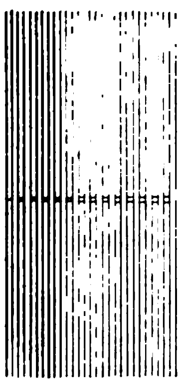

FIG. 28.—Showing uneven break-joint which affects the flux-distribution.

Having regard to all the accidents that may happen, even in the best regulated shops, we may be sure that the iron loss will be greater than the amount calculated by the above formulae. It is therefore well to have curves based upon actual experience from which one can arrive at the probable iron loss. In these curves the hysteresis loss and eddy-current loss can be dealt with together, and the total loss per cubic centimetre or per cubic inch can be read off directly.

The curves in Fig. 29 will be found useful for quickly estimating the iron loss that may be expected in a built-up armature of ordinary manufacture. For the purposes of these curves we have taken sheet iron 0·016″ (or 0·04 cm.) thick, papered with 0·0013″ paper, and assumed that the solidity is 89 %. The flux-density given

as abscissae is the actual flux-density in the iron. Thus the point 10,000 on the abscissa refers to a state of magnetization in which we have a flux-density of 8900 lines per sq. cm. in the built-up mass of iron and paper or 10,000 lines

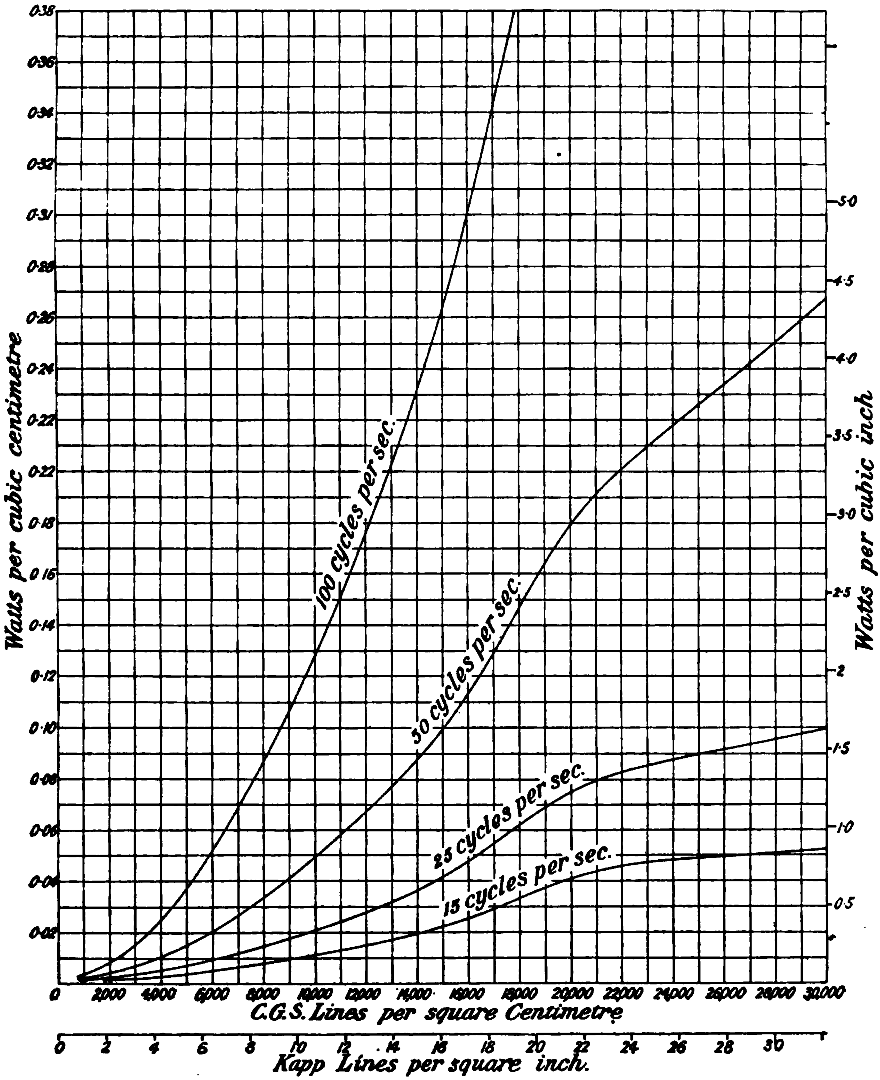

Fig. 29.—Curves for quickly estimating the iron loss in built-up stampings. Thickness of stampings, 0·04 cm.

per sq. cm. in the actual iron. Good commercial armature iron has a hysteretic constant as low as 0·0023, but after it has been punched and assembled we will in general not find the constant much lower than 0·0027. The curves are therefore based on this latter figure. In order to allow something for the short circuiting

of punchings, which always occurs to a certain extent, we have taken the constant 3·7 instead of the constant 2·4 in formula (2), page 49. This constant gives us a figure for the iron loss which agrees with the average case met with in practice. For very carefully built-up armatures with very few short-circuited punchings it is, of course, too high. On the other hand, many cases will be found in practice where it is too low. The curves, then, have been plotted from the formula,

$$\text{watts per cu. cm.} = (0{\cdot}0027 \times n \times K_h) + 3{\cdot}7\,(0{\cdot}04^2 \times n^2 \times B^2_{max} \times 10^{-11}),$$

and in this formula the values for B_{max} are the actual values of the flux-density in the iron obtained by dividing the total flux by the net cross-section of the iron.

It will be noticed that these curves have a curious knee in them, which occurs near the point where B is about 18,000. This knee is produced by the fact that the hysteresis loss does not increase much when we go above this density. The knee is very marked in the curves for 15 and 25 cycles, because in these the eddy-current loss is low as compared with the hysteresis loss. The curves as drawn show us that at low frequencies we can go up to very great flux-densities without being afraid of excessive losses.

It is, of course, impossible to give rules which will give the iron loss very accurately. Two machines may be built to the same drawings and of the same material so far as tests can show, and yet one may have 20 % more iron loss than the other. In cases where a machine has received unfair treatment in punching and building, its iron loss may even be doubled. The constants given above are sufficiently near to obtain figures for the calculation of efficiency. In cases where it is necessary to give the very highest efficiencies, the actual hysteretic constant of the material to be employed may be inserted instead of 0·0027, and the coefficient 3·7 may be reduced to a value as near to 2·4 as is thought safe, having regard to the amount of care that will be exercised in the building and treatment of the core.

Where good silicon steel, containing 3 % of silicon, is employed, it is fairly safe to take the hysteretic constant at 0·0016, but although the specific resistance of the material is four or five times that of ordinary iron, it is not safe to reduce the constant 3·7 to below 1·8 unless experience with similar armatures built with the same care warrants it. Theoretically, with a perfectly built armature of silicon iron, neglecting the losses which occur at the flanks (as to which a separate allowance might be made), the iron loss per cu. cm. in an armature might be reduced to

$$(0{\cdot}001 \times n \times K_h) + 0{\cdot}5\,(0{\cdot}04^2 \times n^2 \times B^2_m \times 10^{-11}).$$

It is to be hoped that the day will come when our methods of treating and building the iron will enable us to always use the last-given formula.

The ordinary method of stating the loss in any given sample of iron, is to give a figure for the sum of the losses due to hysteresis and eddy currents in one pound of the sheet iron when subjected to an alternating magnetic field with a maximum flux-density of 10,000 lines per sq. cm. at a frequency of 50, the thickness of

iron being 0·5 mm. The following list shows how the quality of the iron has been improved during the last few years:

Material.	Loss in watts per lb. under standard conditions stated above.
Dynamo steel in 1893, - -	2·1
Good ordinary (1914), - -	1·7
Better quality (1914), - -	1·3
Silicon steel (3 % Si), - -	0·9
Silicon steel (3·5 % Si), - -	0·8
Silicon steel (4·8 % Si), - -	0·56

The above losses are those which would be measured in the iron when built up in a transformer core. For the reasons given on page 49, when the iron is built up in a machine the losses are usually very much greater, as shown by the curves in Fig. 29. It will be seen, for instance, that for the standard test conditions (50 cycles $B = 10{,}000$) the loss given in Fig. 29 is 0·82 watt per cubic inch. Taking the volume of 3·6 cu. in. of built-up punchings as weighing 1 lb., we have 2·9 watts per lb., or nearly double the figure given above for good ordinary iron. One recognizes, therefore, how important it is to build the iron carefully and keep it free from burrs. The curves given in Fig. 29 are average curves; the losses can be easily exceeded on a badly burred core.

It is the practice of some manufacturers to anneal the sheet iron after punching. This has the effect of preventing the increase of hysteresis loss which may have been occasioned by the straining of the metal under the punch, and it also has the good effect of oxidizing sharp edges burred up by the punch. Very low iron losses have been obtained with sheet metal so annealed, even when the insulation between sheets consisted only of varnish. The objection to using only varnish between the sheets is that the varnish may in the course of time be squeezed out, so that burrs and projections on the punchings make contact with one another, and the iron loss is thereby increased. A solid insulator like paper makes a more permanent spacer between sheets.

It is convenient to paste the paper on the sheet before it is punched, and one cannot anneal papered sheet iron after punching. It is found that if sheet metal is not annealed after punching it builds up more accurately. The punchings are sometimes slightly distorted during the annealing process in a way whith causes them to build up less accurately, and this gives a rougher surface inside the slots.

Silicon steel is frequently rolled to sheets that are rather thicker than the transformer iron of the ordinary sort. The specific resistance is from three to five times as great as ordinary iron, so it is not worth while to roll it so thin. A thickness of 0·5 mm. is common. If the percentage of silicon is increased to 4·8, the resistance goes up to six times that of ordinary iron. Such a high percentage of silicon, however, makes the steel too brittle for use in dynamos.

The curves given in Fig. 30 may be taken as giving average losses per cubic inch alloyed sheet steel (percentage of silicon, 3 per cent.) assembled in an ordinary armature core and subjected to fair treatment. Alloyed sheet steel containing 4 per cent. of silicon is sometimes rather brittle, and is rather difficult

to punch. Moreover, the punchings may become brittle with time even if they are not when the sheet is punched, so that the teeth break off when subjected to bending forces. This is a very dangerous fault. In order to meet this difficulty

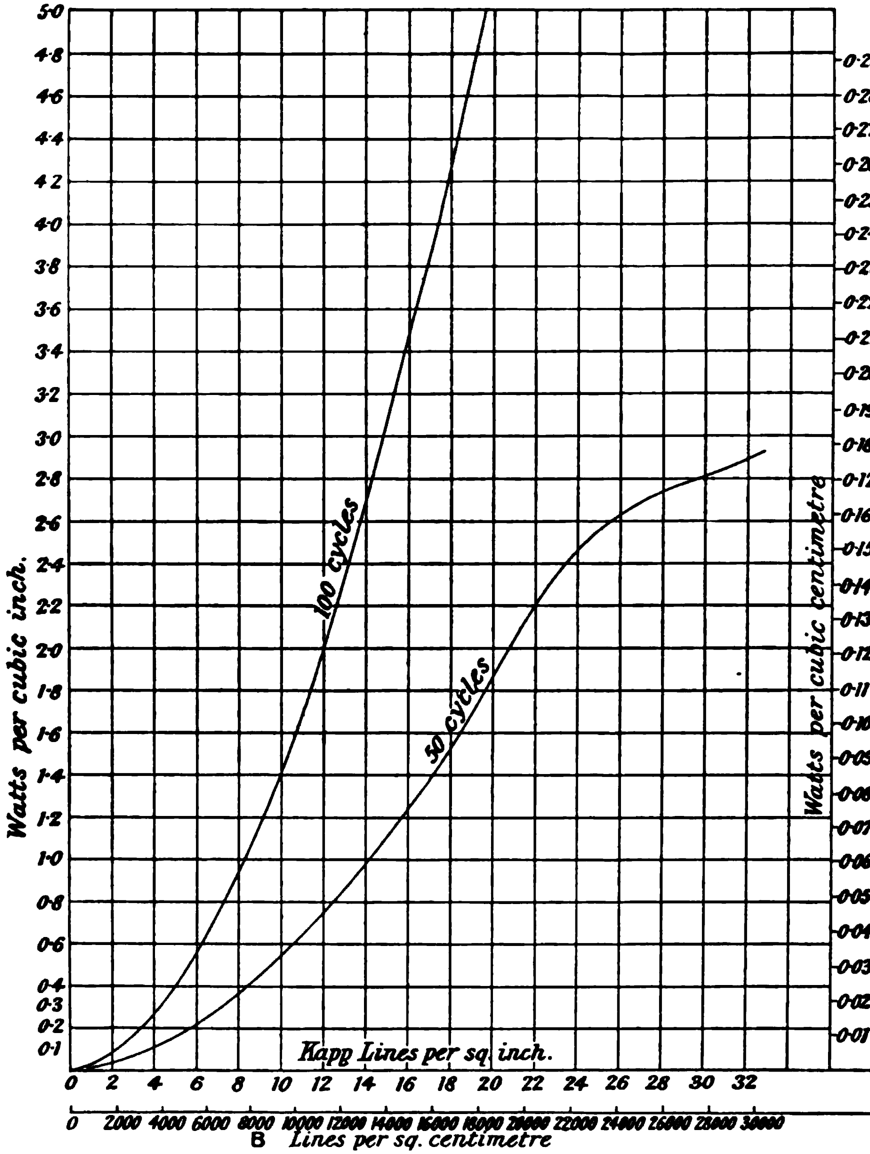

Fig. 30.—Curves for quickly estimating the iron loss occurring in silicon steel (3 per cent. silicon) assembled in an ordinary armature core. Thickness of stampings, 0·05 cm.

some steel manufacturers make an alloyed steel with a rather smaller percentage of silicon (about 3 per cent.). This material, though not at all brittle, has a tensile strength as high as 105,000 lbs. per sq. in., and is therefore very suitable for the rotating armatures of turbo machines.

For references to articles on dynamo steel and iron loss, see page 86.

CHAPTER V.

THE PARTS OF THE MAGNETIC CIRCUIT.

IN this chapter we will collect the rules which are of service in making calculations relating to various parts of the magnetic circuit. The following symbols

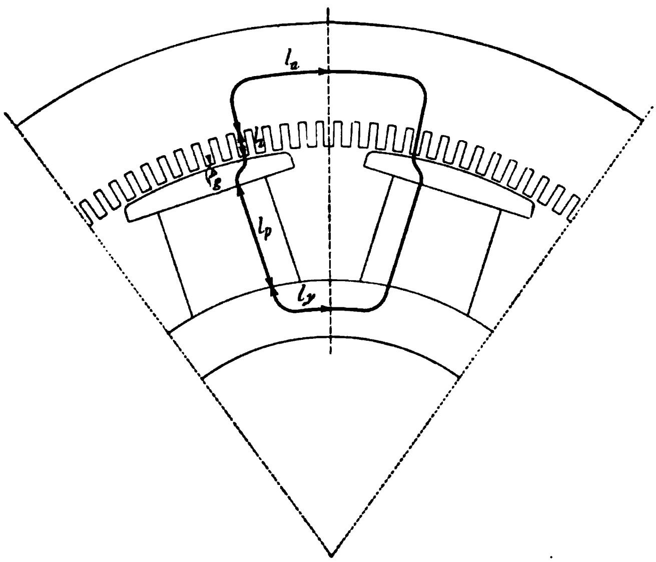

FIG. 31.—Parts of the magnetic circuit.

will be used in this book to denote the lengths of the various parts of this circuit:

g = length of gap,
l_z = length of teeth,
l_a = length in armature core,
l_p = length of pole body,
l_y = length in yoke.

Fig. 31 gives a sectional view for the magnetic circuit of a revolving field A.C. generator.

THE AIR-GAP.

The flux-density in the air-gap. We have seen in Chapter II. how we can plot the flux-density along the pole face in various types of machines. It is usual, in calculating the electromotive force generated in the conductors of an armature, to regard the conductors as moving across a magnetic field in the air-gap of the machine. The results thus arrived at are in the main correct, although the conductors may not be in the air-gap but in slots. The total flux cut per pole is the same whether the conductor is actually in the strong field, in the air-gap, or in a weak field in the slot. We may satisfy our notions of a conductor-moving-in-a-field by saying that the velocity of the weak field in the slot is greater than the velocity of the periphery of the armature in the inverse ratio of the density in the slot to the density in the gap.

Thus, our formula $E = K_e \mathrm{B} A_g Z_s R_{pm} \times \frac{1}{60} \times 10^{-8}$, given on page 7, holds for all machines, whether surface wound or iron clad, and whether the conductors are mechanically driven through the field or the field rotates magnetically as in an induction motor (see p. 304).

The flux-density in the air-gap of a machine may be taken as a convenient criterion of the good use that is being made of the magnetic circuit, and for any given frame its value tells us of the state of saturation of the machine.

Thus, if the flux-density in the air-gap of a certain machine is 30 kilolines per sq. in., and if we find that at full speed we generate 3 volts per conductor and have 60 kilolines per sq. in. in the teeth, 40 in the iron behind slots and 45 in the pole limbs, then with 60 kilolines in the gap we shall generate 6 volts per conductor, and have as a first approximation 120 kilolines in the teeth, 80 in the iron behind slots and 90 in the pole limbs. The gap-density is a convenient quantity to which we can refer the intensity of the magnetic effects in all parts of the machine, although in many cases account must be taken of leakage fluxes, which interfere with a true proportionality between the various quantities.

Its value, moreover, tells us at a glance whether a given frame is being used to its best advantage. We know that in many cases the flux-density in the gap may be as high as 60 kilolines per sq. in. at no load. If the figure is lower than this we will not be satisfied with the design until we have found a sufficient reason for having it so low. Or we may have it above 60, in which case we may get a proportionately higher output from the frame.

What, then, is it that limits the value we may take for the flux-density in the gap? Most commonly it is the excessive saturation that would occur in the iron of other parts of the magnetic circuit if the air-gap density were too great. But this is not always the limiting condition. In some large alternators with a great number of poles and a small air-gap, the flux-density in the gap cannot be increased beyond, say, 50 without making excessive the unbalanced magnetic pull for small displacements of the frame from the true concentric position. In this case the output of the frame may be limited by this consideration. With induction motors too great a flux-density in the gap would call for too great a magnetizing current, and with wound motors the density must sometimes be kept low to prevent an excessive magnetic pull. These matters will be dealt with in their place, and in

the designs worked out in the subsequent chapters the reader will see what consideration it is that limits the value of the flux-density in each particular case.

As the possibility of an unbalanced magnetic pull must be considered in both continuous-current and alternating-current generators and motors, we will deal with it here.

Unbalanced magnetic pull. As long as the armature of a generator or motor remains concentric with the field and the frame does not become distorted, the poles exert an even magnetic pull up and down, right and left, for each carries the same number of ampere-turns. As the upward forces are balanced by the downward forces, the bending moment in the shaft is produced only by the weight of the rotating part. But this is a state of affairs that we cannot always count upon. The bearings may wear and let the rotor down a small fraction of an inch. Some small initial dissymmetry may bring about the springing of the frame, and as the air-gap closes up on one side the magnetic pull there may increase at such a rate that it is able to pull the armature hard up against the field-magnet. Sometimes a dissymmetry in the winding or in the quality of the material is sufficient to start the trouble. It is therefore necessary to calculate how much the unbalanced pull amounts to when we have a small accidental displacement, and make such provision in the design of the shaft and frame as will with certainty prevent a pull-over.

A simple plan is to assume that if the shaft and frame are strong enough to withstand the unbalanced pull which would be caused by a displacement of, say, 1 mm. if we are using C.G.S. units (or, say, $\frac{1}{32}$ inch) from the true concentric position, then it will be strong enough to withstand the accidents of this kind which may happen in service. The assumption enables us to give to the designer of the mechanical parts a definite figure for the magnetic pull, and this figure he adds to the weight and other forces on the parts when calculating the maximum deflexion. This deflexion must in general be well within the 1 mm. or $\frac{1}{32}$ inch as the case may be. We must not, however, forget that special cases may arise in which it is necessary to make provision against displacements greater than 1 mm.

The unbalanced magnetic pull due to a small displacement of the armature. We can deduce by the method given below a convenient formula for calculating the unbalanced magnetic pull.

It should be pointed out, in the first place, that the amount of the unbalanced pull for a given amount of displacement will depend upon the extent to which the iron parts are saturated. If the iron parts of the magnetic circuit are very much saturated, a reduction of the air-gap on one side of the armature will not result in a very great increase in the flux-density in the gap. If, however, there is no saturation, then the flux-density will increase in inverse proportion as the gap is shortened. Other things being equal, the unbalanced pull will be greatest for an unsaturated magnetic circuit. This is the easiest case to calculate, so we will take it first. It is then possible by a simple approximation to allow for the diminution of the pull due to the fact that some of the ampere-turns are expended on the iron.

From first principles we know that the pull on the face of a magnet (made of a material of great permeability), per square centimetre of active face, is

$\frac{B^2}{8\pi}$ dynes. This is easily seen when we remember that the energy stored in a cubic centimetre of air-gap is $\frac{1}{2}\frac{HB}{4\pi}$ ergs (see *Elements of Electricity and Magnetism*, by J. J. Thomson, 1893, p. 266). This may be put into the form $\frac{\mu H^2}{8\pi}$ or $\frac{B^2}{\mu 8\pi}$. As $\mu = 1$, in air, the energy $= \frac{B^2}{8\pi}$. Now imagine that the magnetic pull makes the iron move so that the space that was air-gap becomes occupied by the iron of great permeability, μ. If B remains constant H becomes nearly zero, so that the energy stored per cubic centimetre becomes $\frac{B^2}{\mu 8\pi}$, that is to say, nearly zero.

In order to thus convert the magnetic energy in one centimetre cube of air into mechanical work, it is necessary to move the square centimetre of iron surface through the centimetre, so that the force exerted must be

$$\frac{B^2}{8\pi}\left(1 - \frac{1}{\mu}\right) \text{ dynes.}$$

It is only in the case where the permeability is great that we can neglect the term $\frac{1}{\mu}$. This is not the case when the iron is very highly saturated.

$$\text{The pull in lbs. per sq. in.} = \frac{6{\cdot}45 \times B^2}{8\pi \times 981 \times 453} = 5{\cdot}75 \times 10^{-7} \times B^2.$$

Or, if the flux-density is measured in lines per sq. in.,

$$\text{the pull in lbs. per sq. in.} = 1{\cdot}39 \times 10^{-8} \times (B'')^2.$$

If the flux-density is given in kapp lines per sq. in.,

$$\text{the pull in lbs. per sq. in.} = 1{\cdot}39 \times 10^{-8} \times (6000)^2 \times B_K^2$$
$$= \tfrac{1}{2}B_K^2.$$

The last expression is in such a simple form that we will keep to the B_K units in what follows.

Let M_P equal the magnetic potential between the pole and the armature of a dynamo, the units being so chosen that $\frac{M_P}{g} = B_K$, where g is the length of the gap between the pole and armature in inches.

Now the pull in lbs. per sq. in. $= \frac{B_K^2}{2} = \frac{M_P^2}{2g^2}$.

Consider the difference in pull at two diametrically opposite points at which the gap is $(g - a)$ and $(g + a)$ respectively (see Fig. 32).

Difference in pull $= \frac{M_P^2}{2}\left(\frac{1}{(g-a)^2} - \frac{1}{(g+a)^2}\right)$ lbs. per sq. in.

Consider first the case where a is small compared with g.

Expanding the expression and neglecting quantities much smaller than $\frac{a}{g^3}$, the difference in pull $= \frac{M_P^2}{2} \times \frac{4a}{g^3}$.

But $M = B_K \times g$, therefore the difference in pull per sq. in. $= \frac{B_K^2 g^2}{2} \times \frac{4a}{g^3} = \frac{2B_K^2 a}{g}$.

Referring now to Fig. 32, as θ changes, the distance between the dotted circle and the full circle changes as $a \sin \theta$, and the vertical component of the difference in pull varies as $a \sin^2 \theta$.

Let the area of any pole surface subtended by the angle θ be equal to $A\theta$.

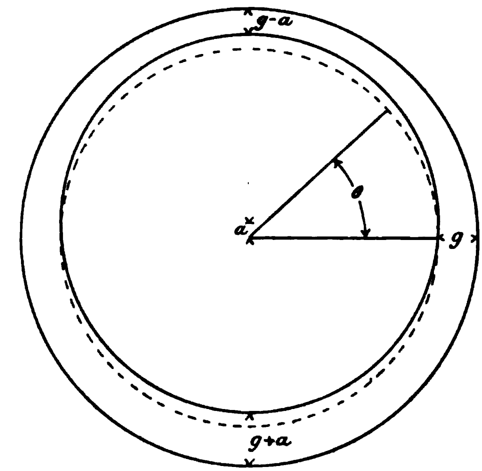

FIG. 32.—Diagram of field-magnet displaced from central position.

Then the total difference in pull taken half-way round the circle

$$=\frac{2B_K^2 aA}{g}\int_0^{\pi}\sin^2\theta\,.\,d\theta=\frac{B_K^2 aA\pi}{g}.$$

Now $A\pi$ is half the total polar surface $=\dfrac{\text{No. of poles}\times\text{sq. in. of pole area}}{2}$.

The difference of pull in lbs. $=0{\cdot}5\times\dfrac{aB_K^2}{g}\times$ No. of poles $\times$ sq. in. of pole area.

$$=0{\cdot}5\times B_K^2\times\text{No. of poles}\times\text{sq. in. of pole}\times\frac{a}{g},$$

where a is very small* as compared with g.

EXAMPLE 7. A 300 K.W. 3-phase generator has 12 poles. Each pole face has an area of 70 square inches. The length of the air-gap is 0·18″ and the normal flux-density in the gap is 58,200 lines per sq. in. Find the unbalanced magnetic pull when the field is displaced $\frac{1}{32}$″, assuming that there is no saturation of the iron.

58,200 lines per sq. in. $=9{\cdot}6$ kapp lines per sq. in.

Unbalanced pull in lbs. $=0{\cdot}5\times 9{\cdot}6\times 9{\cdot}6\times 12\times 70\times\dfrac{0{\cdot}03125}{0{\cdot}18}$

$=6760$ lbs.

* Or, to be more accurate for gaps of 0·1 inch and under, where $\frac{a}{g}$ is not necessarily very small, we should preserve the expression $\dfrac{1}{(g-a)^2}-\dfrac{1}{(g+a)^2}$.

Then the difference in pull in lbs.

$$=0{\cdot}125\,B_K g^2\left(\frac{1}{(g-a)^2}-\frac{1}{(g+a)^2}\right)\times\text{No. of poles}\times\text{sq. in. of pole area.}$$

If $t=$ the number of thirty-seconds of an inch in the gap, then, for a displacement of one thirty-second of an inch from the central position, the unbalanced pull in lbs.

$$=0{\cdot}5\,B_K^2\times\text{No. of poles}\times\text{sq. in. of pole area}\times\left(\frac{1}{t}+\frac{2}{t^3}\right).$$

If the flux-density is given in lines per square inch, we must change the constant 0·5 to $1 \cdot 39 \times 10^{-8}$.

Or, if the flux-density is given in lines per sq. cm. and the area of the pole in sq. cms., the magnetic pull in kilograms is

$$4 \cdot 05 \times 10^{-8} \times B^2 \times \text{No. of poles} \times \text{sq. cm. of pole} \times \frac{a}{g}.$$

EXAMPLE 8. A certain 3-phase generator driven by a gas-engine has 60 poles each having an area of 450 sq. cms. We want to have about 5000 ampere-turns on the pole at no load on the air-gap, and it is required to keep the unbalanced magnetic pull due to a displacement of 0·1 cm. less than 13,000 kilograms. What is the maximum flux-density in the gap that we can employ and what will be the approximate length of gap, assuming no saturation of the iron?

We have, in the first place, $g = \frac{5000 \times 1 \cdot 257}{B}$.

Therefore $13000 = 4 \cdot 05 \times 10^{-8} \times B^2 \times 60 \times 450 \times \frac{0 \cdot 1 \times B}{5000 \times 1 \cdot 257}$,

$$B = 9100, \quad g = 0 \cdot 69 \text{ cm.}$$

The effect of saturation on unbalanced pull. In the above we have assumed that all the ampere-turns are expended for the air-gap. Now, let us see what modification is necessary where a considerable percentage of the ampere-turns are expended on the iron parts of the circuit.

In the case of very large slow-speed generators, the number of poles being great and the economical air-gap small, the unbalanced magnetic pull would often be almost too great to cope with if it were not for the fact that the actual pull is very much less than the pull calculated by the simple formula given above. We cannot, in these cases, neglect the effect of saturation. A simple graphic construction enables us to allow for its effect with sufficient accuracy for practical purposes. This construction is based on the argument that whatever the amount of the unbalanced pull may be under the effect of saturation, we can always imagine an air-gap of such a size that the unbalanced pull would be the same, with the same flux-density and no saturation. The object of the graphical method is to find out what the length of this equivalent gap is. The method is most easily understood from an example worked out.

Fig. 347 gives the no-load magnetization curve of the 1800 K.V.A. three-phase generator, particulars of which are given on p. 357. The length of the air-gap (½ total gap) is 0·51 cm. The flux-density at 6600 volts no load is 9160 C.G.S. lines per sq. cm. It is required to find out what the unbalanced pull will be when the centre of the rotor is displaced by 0·1 cm. from the central position.

First find how many ampere-turns are required to drive the flux in the gap across 0·1 cm. We have

$$0 \cdot 8 \times 9160 \times 0 \cdot 1 = 733 \text{ ampere-turns.}$$

Draw the two small vertical lines, as shown at c and d, near the working part of the magnetization curve. Take as the horizontal distance between these lines the distance on the horizontal scale that represents 733 ampere-turns. Now draw a chord to the curve through the two points intersected by these lines, and through the origin draw the line Ob parallel to this chord. Let this line Ob intersect at the point b the horizontal line ab, drawn at a height to represent the working

flux-density or voltage. Then the ratio of the line cd to the line ab is the ratio of 0·1 cm. to the length of the equivalent gap. For it is easy to see that if the gap were so great as to require 11,900 ampere-turns at no load, the increase of the flux for a decrease of the gap of 0·1 cm. would be the same as the increase of the flux in the actual machine for a decrease of the gap of 0·1 cm. We can therefore employ the same formula as before, except that instead of the ratio $\frac{a}{g}$ we use the ratio $\frac{a}{g_1}$, where g_1 is the length of the equivalent gap as defined above. In our example we get $\frac{a}{g_1}$ from the ratios of the ampere-turns $\frac{733}{11,900} = 0{\cdot}061$, so that our formula gives us

$$4{\cdot}05 \times 10^{-8} \times 9160 \times 9160 \times 40 \times 650 \times 0{\cdot}061 = 5400 \text{ kilograms.}$$

If we had neglected the effect of saturation, our formula would have given us

$$5400 \times \frac{0{\cdot}2}{0{\cdot}061} = 17,700 \text{ kilograms.}$$

Permissible amount of unbalanced magnetic pull. In very large engine-type alternators the designer of the mechanical parts should be provided with a curve showing how the magnetic pull varies as the displacement varies from zero to (say) 0·1 in.

In order to give a general idea as to how great a magnetic pull is permissible, we may say that the unbalanced magnetic pull with $\frac{1}{32}''$ displacement in the case of engine-driven alternators of from 100 to 500 K.W. capacity may be as great as the weight of the rotating part, but to have it as high as this necessitates the use of a rather strong shaft. Usually the unbalanced pull does not amount to so much.

Effect of circuits in parallel on unbalanced pull. In the case of C.C. armatures with a number of circuits in parallel, particularly where a great number of equalizing connections are employed, there cannot be any great magnetic pull when the machine is rotating, because if the field were stronger on one side than on the other it would set up currents in the windings of the armature which would tend to weaken the field on the side where the small air-gap tends to make it strong and strengthen it on the side where it would otherwise be weak. It is therefore usual to neglect the unbalanced magnetic pull in such machines. But it must be remembered that such armatures may be subjected to an unbalanced pull when stationary if they are separately excited. Similarly, all armatures, whether for A.C. generators or induction motors, having several circuits in parallel, or having a short-circuited winding as a squirrel cage or amortisseur on the rotating element, cannot have a much stronger field threading through one part of the circuit than threads through the other. Sometimes alternator armatures are wound with a number of circuits in parallel with the express object of neutralizing the unbalanced magnetic pull (see page 452).

It is not sufficient to make several circuits in parallel on the rotor of an induction motor if we wish to obviate the unbalanced pull, because the slip may be so small that the resistance of the rotor circuits will not permit a sufficient equalizing current to flow.

Length of air-gap. In the calculations of the different machines given in the subsequent chapters, the reasons for fixing the length of air-gap at the chosen value will be made clear. It is only necessary here to briefly state the various considerations which influence the designer in settling upon the length of air-gap.

(1) *To get sufficient magnetomotive force on the field-magnet.* In A.C. generators and C.C. generators and motors without compensating windings, it is necessary to have the magnetomotive force of the field coils much greater than the magnetomotive force of the armature, in order to secure good regulation and good commutation. For this reason the air-gap is often made very much greater than it otherwise would be. For instance, one sometimes sees air-gaps of 2 or 3 inches in turbo-generators which have very few poles.

(2) *To keep down the iron loss.* In machines with open slots, either on the rotor or stator, the air-gap must not be reduced below a certain minimum or the iron loss will be excessive. This must be taken into account not only in small C.C. machines with solid poles, but in all machines, whether the poles are laminated or not. Where any iron is made to pass in front of a number of magnetized iron teeth with only a short air-gap intervening, there is a change in the flux-density which has the frequency of the passage of the teeth, and this is usually much higher than the frequency of the passage of the poles, and gives rise to excessive iron losses.

(3) *Mechanical considerations.* The air-gap must always be large enough to obviate any danger of the rotating part coming in contact with the stationary part, either from the wearing of the bearings or from the springing of the shaft or frame under the action of magnetic pull or other forces to which the machine is subjected. The air-gap in railway motors and in induction motors is fixed by this consideration.

(4) *To ventilate and obviate noise.* In some machines, such as turbo-generators, in which a great deal of air must pass along the gap, the minimum length is sometimes determined by this consideration. Where the pole faces of a machine are large and occupy a great proportion of the space around the armature, the air from the ventilating ducts in the core would not find a sufficiently easy path if the air-gap were made as small as perhaps it might be made from other considerations. Even when numerous air-ducts are provided in the stator core, the air-gap must not be made too small or the blowing of the air against small projections on the stator or rotor will cause excessive noise.

(5) *To keep down the value of the unbalanced magnetic pull.* We have seen above (page 59) that the unbalanced magnetic pull for a small displacement from the concentric position is inversely proportional to the length of the air-gap. In very large engine-driven alternators in which the magnetic pull is a determining factor in the design, special consideration must be given to the length of gap and its influence on the amount of the pull. Where a small flux-density is employed in the gap, a greater gap can be employed with a given number of ampere-turns on the pole (see page 347).

The ampere-turns on the air-gap. The simplest case to consider is where the face of the armature is smooth and the pole is free from ventilating ducts and

slots. The ampere-turns required to create a flux-density of B lines per square centimetre across a gap of g centimetres is

$$\frac{10}{4\pi} \times Bg \quad \text{or} \quad 0{\cdot}795Bg.$$

Or, if we are working with dimensions in inches and with B'' measured in lines per square inch, then

$$\text{ampere-turns on the gap} = 0{\cdot}313 \times B'' \times g''.$$

EXAMPLE 9. In a certain generator the value of $B''A_g$ was 300 megalines. Taking A_g as 4900 sq. in., we get $B''=61{,}000$ lines per sq. in. If the gap is 0·375 inch, then

$$\text{ampere-turns on the gap} = 0{\cdot}313 \times 61{,}000 \times 0{\cdot}375 = 7200.$$

Or, again, if we wish to work in kapp lines and have B_K kapp lines per square inch, then

$$\text{ampere-turns on the gap} = 1880 \times B_K \times g''.$$

It will be seen that method of calculation described on page 7 obviates all the calculations as to the number of teeth per pole or the area of the pole, or the density of the flux under different parts of the pole. It takes into account only the maximum density in the gap, and leaves to the constant K_e the duty of making allowances for the width of the pole, the bevel of the pole and other matters which affect the total electromotive force generated.

It should be remembered that very often the calculation given above is reversed in practice. It is often necessary, say for the purpose of securing good regulation, to apply a given number of ampere-turns to the pole. In this case we make the air-gap great enough to call for the desired number of ampere-turns.

EXAMPLE 10. In a certain A.C. generator it is desired to have 10,000 ampere-turns per pole. It is also desired to throw 10% of the ampere-turns at no load on the teeth for the purpose of getting the desired saturation. Deducting 10% from the 10,000, we have 9000 for the gap. Assume that the flux-density is 61,000, as in the last example,

$$9000 = 0{\cdot}313 \times 61{,}000 \times g'',$$

$$g'' = 0{\cdot}47 \text{ in.}$$

The effect of open slots and ventilating ducts. Next consider the case where there are open slots in the armature. The effect of the open slots is to increase

FIG. 33.—Showing how magnetic flux from armature teeth distributes itself in the air-gap.

the reluctance of the air-gap. The lines of force crowd into the tops of the teeth in the manner indicated in Fig. 33. The effect of the slots in thus contracting the

path of the flux across the gap depends upon the ratio between the width of the slot and the length of the gap.* We may calculate the amount of the contraction

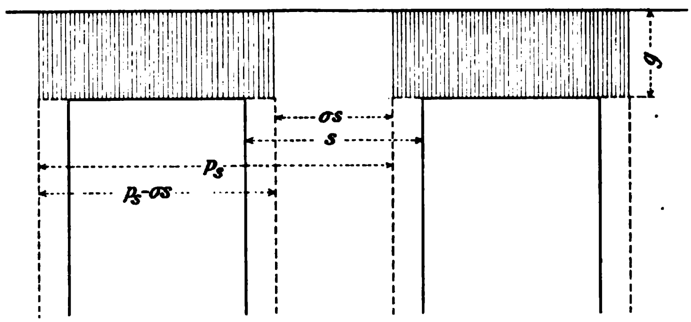

FIG. 34.—Explaining the convention upon which the curves in Figs. 36 and 37 are based.

of the path by considering that, for a region somewhat narrower than the slot, no flux passes at all, and that for the remainder of the pitch of the slots the flux is uniform, as shown in Fig. 34. The values of the coefficient σ, by which we must

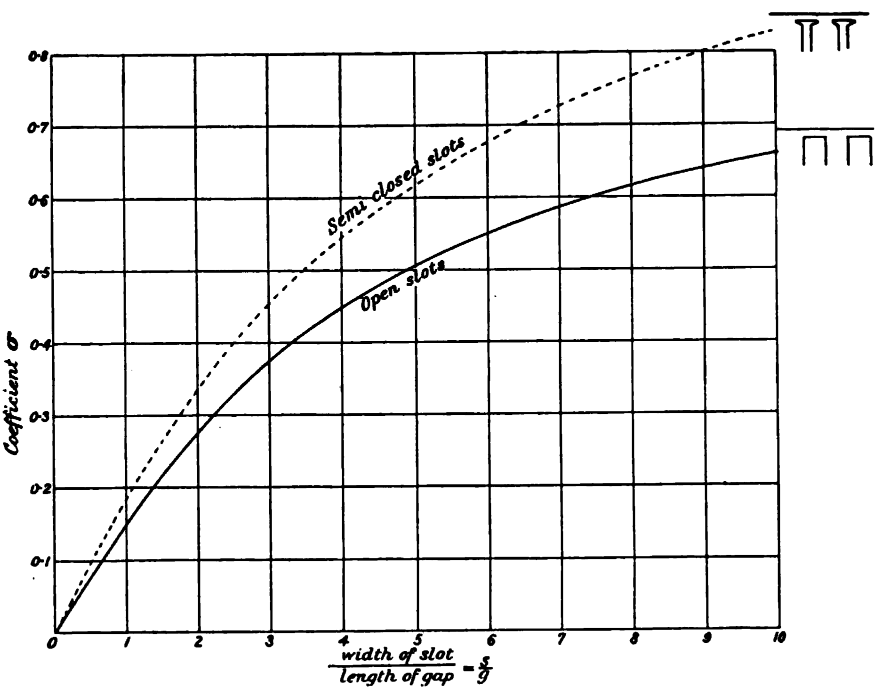

FIG. 35.—Curves giving the values of σ for various ratios of width of slot to length of gap.

multiply s in order to get the virtual value of the slot width on this assumption, are given in Fig. 35 for different ratios of slot width to gap length.

* See papers by F. W. Carter, *Journal of Inst. Elec. Engineers*, vol. 29, p. 925, and vol. 34, p. 47; also *Elec. World and Engr.*, Nov. 30, 1901; Hawkins and Wrightman, *ibid.*, vol. 29, p. 436; Hele-Shaw, Hay and Powell, *ibid.*, vol. 34, p. 21.

The full-line curve gives the value of σ for rectangular open slots. The dotted curve gives the value of σ for semi-closed slots of the kind usually met with in practice. Strictly speaking, the value of σ depends upon the shape of the lips of the teeth, but for practical purposes the two curves given are sufficient.

Taking the value $s\sigma$ and deducting it from the slot pitch p_s, we get the effective tooth width shown in Fig. 34. On the assumptions made in Fig. 34, the flux-density in the air-gap is increased by the presence of the slot, so that instead of being B it becomes $B \times \frac{p_s}{p_s - s\sigma}$. It is convenient to have curves, such as those given in Fig. 36, from which we can take the **contraction coefficient** K_g without calculation. This curve is used in the following way: Suppose that in the example given on page 23 B″ is 60,000, the length of the gap 0·2″, the width of the slot 0·4″ and the pitch of the slot 0·8″. We find the abscissa $\frac{0{\cdot}4}{0{\cdot}2} = 2$. From 2 we run up the perpendicular until we come to the curve $\frac{s}{p_s} = \frac{0{\cdot}4}{0{\cdot}8} = 0{\cdot}5$. The ordinate of this curve where it meets the perpendicular 2 is 1·16. Therefore $60{,}000 \times 1{\cdot}16$ is the apparent flux-density in the gap, so that the ampere-turns in the gap under the above conditions would be

$$0{\cdot}313 \times 60{,}000 \times 1{\cdot}16 \times 0{\cdot}2'' = 4360.$$

The ampere-turns are 16 per cent. higher than they would be if there were no open slots. If there are ventilating ducts in either the rotor or the stator, or both, we can find the apparent contraction in the flux path caused by them in the same way, as will be seen from the following. Let the gross length of core in the above example be 10″, and let there be four ventilating ducts, each $\frac{1}{4}$″ wide. Here we have $\frac{\text{width of duct}}{\text{length of gap}} = \frac{0{\cdot}25}{0{\cdot}2} = 1{\cdot}25$. The sum of the widths of the ducts is 1″, and as these are spaced over 10″, we may take $\frac{s}{p_s} = \frac{1}{10}$. Now take the curve $\frac{1}{10}$th where it cuts the perpendicular from 1·25. This gives us the ordinate 1·02, the contraction ratio due to the effect of ducts, so the ampere-turns become

$$0{\cdot}313 \times 60{,}000 \times 1{\cdot}16 \times 1{\cdot}02 \times 0{\cdot}2'' = 4450.$$

Similarly, if there are ducts in the stator as well as in the rotor, the contraction ratio for these is found in the same way. The usual practice is to find separately the contraction ratios for the rotor slots and ducts and the stator slots and ducts respectively, then the product of all four gives the total contraction ratio. After a little experience with Fig. 36, an allowance for the smaller contraction ratios due to the ducts can be estimated correctly enough without referring to the curve. The designer allows 2 or 3 per cent. contraction for the rotor ducts, 2 or 3 per cent. for the stator ducts and perhaps 3 or 4 per cent. for some slots in the pole face, and only in those cases when he knows that the nature of the slots necessitates a careful calculation of the contraction ratio does he refer to the figure at all.

The curves given in Fig. 36 refer to the case where open slots are used. Where the slots are of the form commonly found in induction motors with overhanging lips, the values of K_g are higher particularly in those cases where the

mouth of the slot is wide and the air-gap short. If the tooth has the general form illustrated in Fig. 42, we may take the values of σ from the dotted curve given in Fig. 35. In this case it is the mouth of the slot that we must multiply by σ.

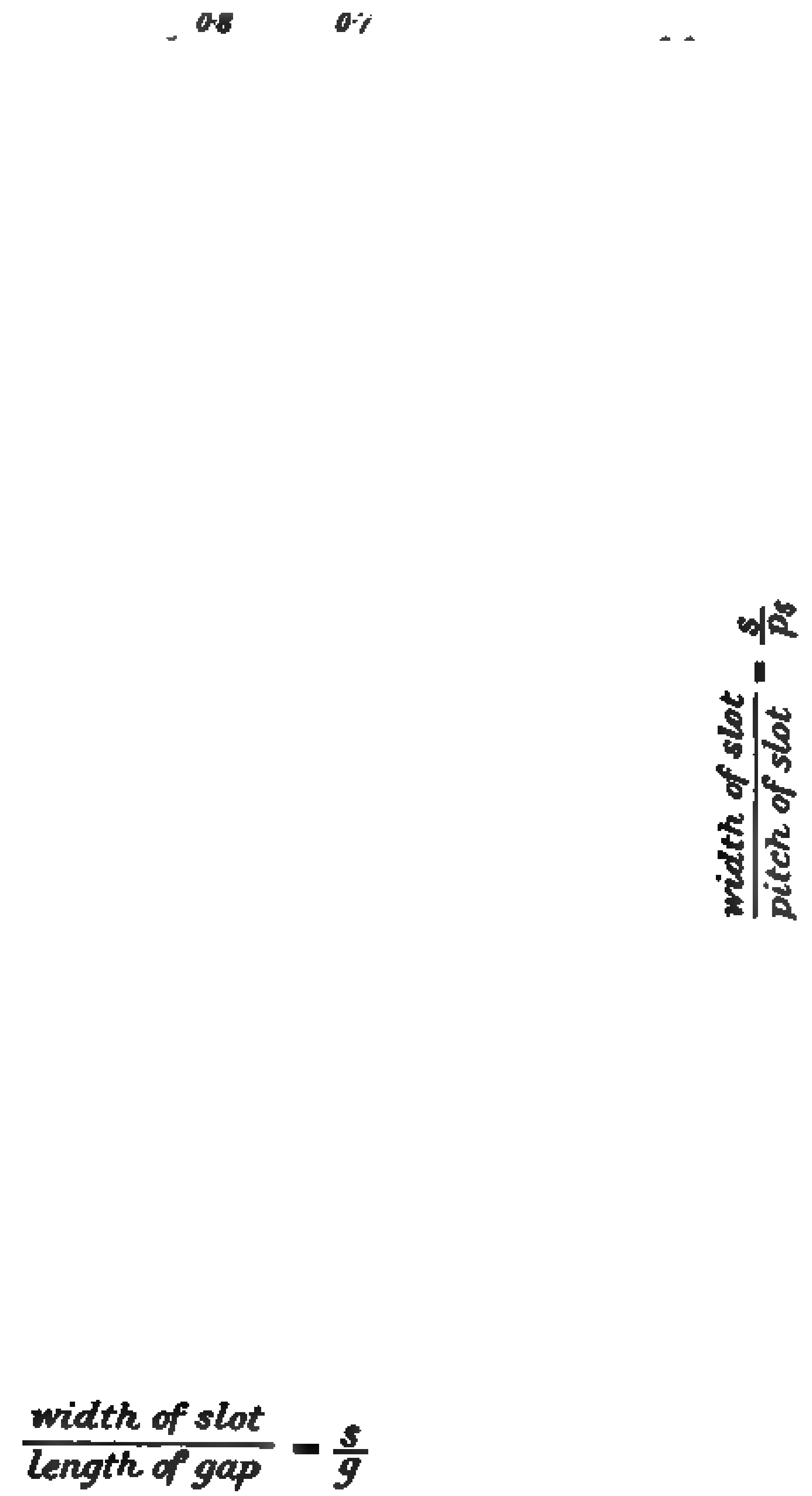

FIG. 36.—Curves for quickly finding the contraction ratio K_p for open slots.

The contraction ratios with semi-closed slots of the shape depicted in Fig. 42 for different values of s and g can be obtained at once from Fig. 37. It will be seen that the values do not differ so widely from those given for open slots as to make it worth while to consider intermediate shapes of slot.

An example of the use of these curves is given on page 417.

The armature windings of nearly all modern alternating-current and continuous-current machines are placed in slots in an iron core. In some cases the

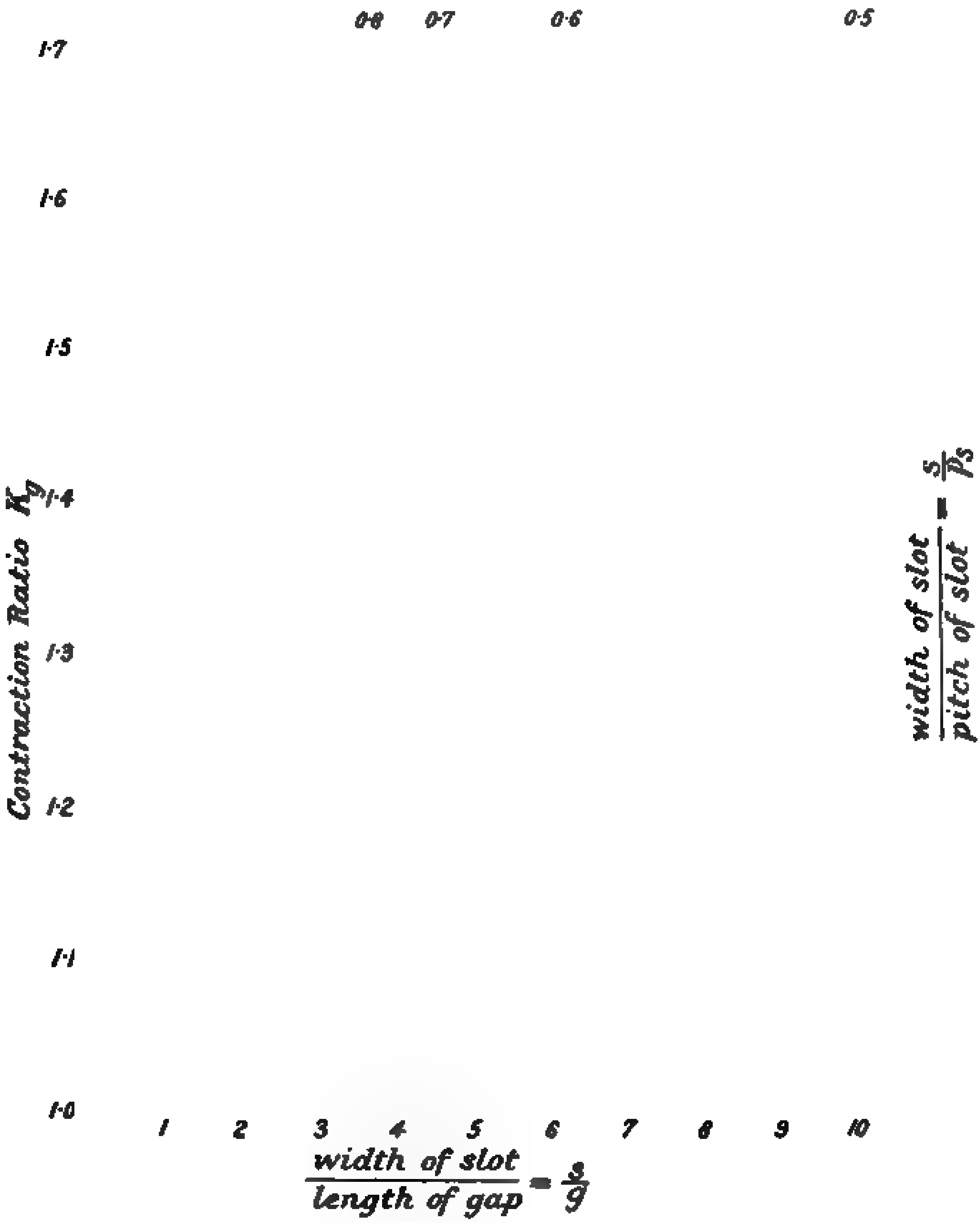

FIG. 37.—Curves for quickly finding the contraction ratio K_g for semi-closed slots.

field windings are so placed. The main reasons for placing windings in slots are:

(1) To give mechanical support and the protection of an iron-clad surface.

(2) To transfer to the iron teeth the forces which would otherwise come upon the conductors.

(3) To reduce the reluctance of the magnetic circuit.

The circumstances which settle the size and shape of slots vary according to the class of machine with which we are dealing. As each class of machine is considered in its place,* these circumstances will be considered at length.

In general, if we wish to make the ampere-wires per inch great, we would like the slots to be large and roomy. On the other hand, to increase the magnetic loading of the machine, we should like to make the teeth wide and the slots narrow. These two courses being inconsistent with each other, we must make a compromise, and considerable judgment is required to make the best use of the room at our disposal.

Choice of number of slots. In high-voltage machines (10,000 volts and upwards) the slots will be made as few as possible and as large as possible, so that the space occupied by the thick containing walls of insulation shall not take up too great a proportion of the total space available for the winding. The drawback to having too few and too large slots is that the cooling surface of the coils will be small compared with the total cross-section of the coils. Moreover, the wave-form of the machine may be prejudicially affected.

In low-voltage machines, where the insulation does not occupy so much room, the tendency will be to increase the number of slots, so as to obtain a large cooling surface. In C.C. machines, the number and size of the slots are usually settled by the commutating conditions (see page 479).

Depth of slots. The question naturally arises as to what fixes the depth of the slots. In C.C. and A.C. generators, it is usually necessary to preserve some stated ratio between the ampere-wires on the armature and the ampere-turns on the field-magnet. Very frequently the ampere-turns on the field-magnet are limited by the copper and iron space available, so that a limit is fixed to the ampere-wires per inch on the armature. It is therefore not necessary or desirable to make the slots any deeper than is sufficient to accommodate the copper which will carry this electric loading. Where the ampere-wires per inch of periphery are not limited by considerations such as these, it is possible to increase the depth of the slots until the leakage across the slots on full load begins to bear too great a ratio to the working flux of the pole. This ratio of the leakage flux to the working flux is the main consideration which determines the depth of the winding space in field-magnets. For instance, in the case of revolving field-magnets for engine-driven alternate-current generators, it is found that no advantage in output is to be obtained by making the radial length of the poles more than $2\frac{1}{2}$ times the width of the pole, because the increased leakage between the poles neutralizes any advantage that we can obtain from the increased winding space. This figure for the ratio between the radial length of the pole and the width of the pole of course differs in different circumstances (see page 300).

When we come to consider the field-magnets of A.C. turbo-driven generators of the cylindrical type, we shall see that the limit in depth of the slots is sometimes fixed by the amount of room which it is necessary to leave behind the slots. The copper space on the rotor being thus limited, the ampere-turns on the armature are likewise limited, and the depth of the armature slots will be made just great enough to accommodate the requisite copper conductors. Otherwise, in the case of

* For slots of A.C. generators, see page 322; of induction motors, see pages 422 and 453; of C.C. generators, see pages 480 and 490; also page 533.

an external armature, there is hardly any limit to the depth which might be chosen for the armature slots. The greater depth would, of course, increase the armature self-induction, but very often it would be an advantage to have this increased.

In induction motors, the depth of the slots is an important factor in determining the leakage flux, on which the performance of the motor greatly depends (see page 422).

Very often the amount of electric loading on an armature is limited by the cooling conditions on the end connectors. A deep slot will often necessitate an arrangement of copper on the end connections which makes the cooling difficult. This is a consideration which sometimes makes it desirable to use a shallow slot.

In direct current machines, the possible depth of the slot is sometimes determined by the commutating conditions, and there is very little doubt that where commutating poles are used, the slots can be made much deeper than in machines without commutating poles.

Owing to the above considerations, one will generally find in practice that the depth of an armature slot is not greater than one-fifth of the pole pitch, and in good regulating machines of conservative design, the depth is often not more than one-tenth of the pole pitch.

Forms of slots. Open slots. It is very convenient in many machines to form the coils beforehand and put them into open slots. Where the coils are secured

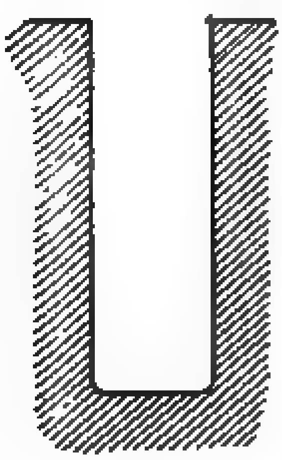

Fig. 38.

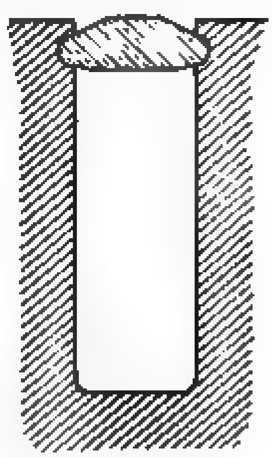

Fig. 39.

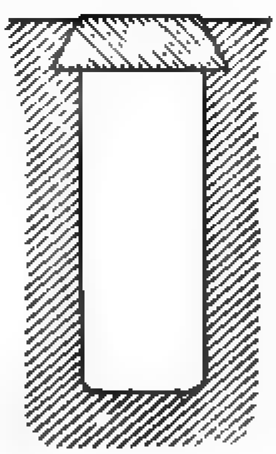

Fig. 40.

Fig. 41.

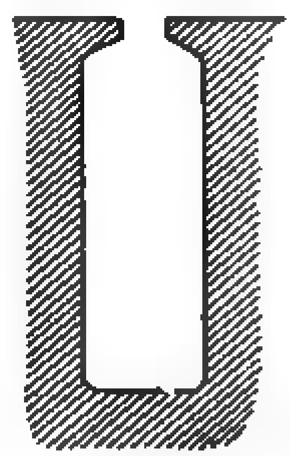

Fig. 42.

by banding, the slots may be made of the simple rectangular form shown in Fig. 38. Where it is intended to secure the coils by means of a wedge of paper or wood, the slot may have any of the forms shown in Figs. 39, 40 or 41. The form in Fig. 39 has the advantage of requiring only a small and simple form of wedge. The form in Fig. 40, however, gives a stronger wedge for the space occupied, and is for that reason often used in turbo-generators. Where the slot is very wide and a still stronger wedge is required, the form shown in Fig. 41 is useful. In this case, the coil is too wide to be inserted as a whole. It commonly consists of two or four conductors, or it may be two or four coils, each of which is inserted separately.

The magnetic leakage across an open slot will be smaller than the leakage across a semi-closed slot, but, as we have seen on page 65, the reluctance of the air-gap is greater with an open slot, and the loss in the pole face is greater, particularly when the air-gap is short.

Semi-closed slots. For short air-gaps, and in cases where it is required to reduce the ampere-turns to a minimum, semi-closed slots, such as illustrated in Fig. 42, are widely used. The addition of the lips makes the process of winding much more difficult. Nevertheless, the great advantages to be obtained in some induction motors and some alternate-current generators has brought the semi-closed slot into very wide use.

As slots are placed around the periphery of an armature, as shown in Fig. 43, their medial lines *mm* cannot be parallel, and on small armatures there is often a very considerable angle between one slot and the next. The question, therefore, arises whether it is better to make the sides of the slot parallel and the sides of the teeth converging, or *vice versa*. In cases where the slots are open and adapted to take coils of rectangular section, slots with parallel sides will be employed. In this case the sides of the teeth will not be parallel. Where the armature is the revolving element, they will be narrower at the root and wider at the periphery. Where the armature is the stationary element, they will be wider

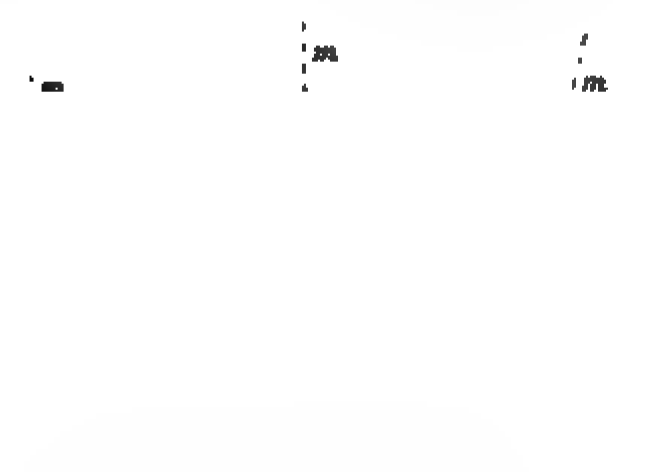

FIG. 43.—Showing how parallel slots lead to taper teeth, especially where the slots are few in number and large as compared with circle on which they are placed.

FIG. 44.—Showing parallel teeth and taper slots, and the manner of utilizing the space in taper slots.

at the root and narrower at the periphery. From the magnetic point of view, teeth narrow at the root are not as good as parallel teeth. The flux at the root is generally as great as or greater than the flux at the periphery, and the ampere-turns required on a taper tooth when highly saturated are very much greater than for parallel teeth of the same mean cross-section carrying the same flux. This will be seen from the example worked out on page 76. To make the best use, therefore, of available space, one would prefer to use parallel teeth and slots with converging sides, but the difficulties which this arrangement would ordinarily entail have led to the more common use of parallel slots. In those cases, however, where mush windings (*i.e.* windings consisting of a large number of small wires placed haphazard in an insulated slot) are used, and where the exact form of the coil is of little importance, considerable advantage can be obtained by employing a parallel tooth and tapered slots (see Fig. 44). Again, in the case of bar-wound armatures or field-magnets, where there are comparatively few bars per slot, it may in some cases be an advantage to shape the bars so as to fit into tapered slots. Such an arrangement is shown in one of the slots in Fig. 44.

The flux-density in the teeth. When the teeth are highly saturated a considerable portion of the flux finds its way down the slots and the ventilating

ducts, so we must consider the teeth, slots and ducts as constituting magnetic paths in parallel. For shortness of expression, we shall speak of the teeth as "iron," and the slot space, duct space and space occupied by the insulation between the iron sheets as "air." In Fig. 45 we are supposed to be looking down on the top of slots. We can draw a rectangle $ABCD$ around the space occupied by one tooth and one slot between two ventilating ducts and as much of a duct as lies within one tooth pitch. In Fig. 45 a small rectangle is portioned off to represent the space occupied by the paper or other insulation. The ratio of the whole area $ABCD$ to the area of the iron $EFCG$ we shall denote by $K_s = \frac{\text{iron} + \text{air}}{\text{iron}}$. On a machine of large diameter, where the sides of the teeth are nearly parallel, K_s is almost a constant for any section through the teeth.

On small armatures with teeth much tapered, as in Fig. 43, it is very far from constant, being perhaps 10 per cent. greater for a section through the root of the teeth than for a section through the tops of the teeth. No very great error is introduced in regarding K_s as a constant if its value is calculated at a point distant by one-quarter of the length of the tooth from the narrowest part of the tooth. Thus, the method of calculating K_s for a revolving armature is as follows: From the diameter d_a subtract $1\frac{1}{2}$ times the length of the teeth l_t, and multiply by π to get the mean circumference $\pi(d_a - 1{\cdot}5l_t)$. From this subtract the number of slots multiplied by the width of the slots, and multiply the difference by the net length of iron. This gives us the total section of iron in all the teeth. Then multiply the quantity $\pi(d_a - 1{\cdot}5l_t)$ by the gross length of the armature core to get the total section of iron and air. The ratio K_s is then obtained by dividing the latter section by the former section.

FIG. 45.—Showing the meaning of the coefficient K_s used in Figs. 46 and 47.

EXAMPLE 11. A continuous-current armature is 120″ diam. and is 13″ long. It has 324 slots, each ½″ wide and 2″ deep. There are 4 ventilating ducts, each ⅜″ wide. What is the section of all the teeth and the value of K_s, taking the solidity of the iron as 89%?

$$(120 - 3)\pi = 368$$
$$324 \times 0{\cdot}5 = 162$$
$$\overline{206}$$

Net length of iron $(13 - 1{\cdot}5)0{\cdot}89 = 10{\cdot}2$,

$206 \times 10{\cdot}2 = 2110$ sq. in. of solid iron,

$368 \times 13 = 4784$ sq. in. of air and iron,

$$K = \frac{4784}{2110} = 2{\cdot}28.$$

In the above example, suppose that A B″ = 300 megalines. Then the apparent flux-density on the teeth will be $300 \times 10^6 \div 2100 = 143{,}000$ lines per sq. inch.

The state of saturation of the teeth will in this case be so high that a considerable percentage of the flux will find its way by the slots and ducts.

The curves in Fig. 46 show the relation between the actual flux-density in the iron and the apparent flux-density for different values of K_s. These curves are easily plotted for any magnetic material as follows: Write down from the

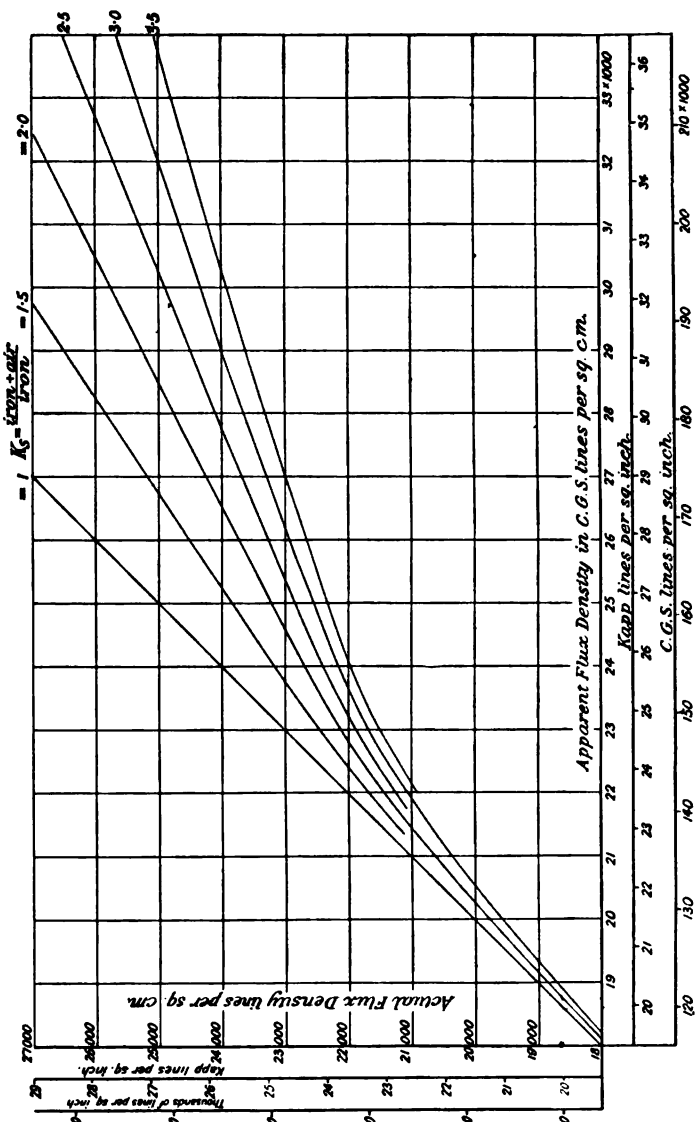

FIG. 46.—Curves giving the relation between apparent flux-densities and actual flux-density for different values of K_s.

magnetization curve a list of the values of B and H. Draw on squared paper the line for $K_s = 1$ at 45° through the origin. If $K_s = 1$ there are no slots or vents, and the armature is the same as if of solid iron; the apparent flux density is equal to the actual flux-density. Now, for $K_s = 2$ we have the air space in Fig. 45 equal to the iron space, so that when the actual B = 22,000 and H = 800 the slot space

carries 800 lines per sq. cm., which, when added to the 22,000, gives us 22,800 apparent flux-density. When plotting the curve for $K_s=2$, therefore, it is only necessary to add the values of H to the abscissae of the curve for $K_s=1$. Similarly, the curve for $K_s=1\cdot5$ is obtained by adding half the values of H to the curve for $K_s=1$.

EXAMPLE 12. An armature is 200 cms. in diam. and is 25 cms. long. It has 288 slots, each 1·04 cms. wide and 4 cms. deep. There are three vents, each 1 cm. wide. What is the section of all the teeth and the value of K_s?

$$(200-6)\pi=610$$

$$288\times1\cdot04=\underline{300}$$

$$\overline{310}\times(25-3)=6850$$

$$6850\times0\cdot89=6100 \text{ sq. cms. of iron,}$$

$$610\times25=15{,}250 \text{ sq. cms. of air and iron,}$$

$$K_s=\frac{15250}{6100}=2\cdot5.$$

Suppose now that the total flux of the frame $A_s\text{B}=140$ megalines. Then the apparent flux-density in the teeth is

$$140\times10^6\div6100=23{,}000.$$

To find the actual flux-density from Fig. 46, follow up the perpendicular from 23,000 apparent B to the 2·5 line, and we get actual B$=21{,}900$.

Where the diameter of the armature is great as compared with the size of the teeth, so that the sides of the teeth are nearly parallel, it is sufficient to calculate B in this way, and from the magnetization curve find the ampere-turns per centimetre, which, when multiplied by the length of the tooth, gives the ampere-turns on the teeth.

For instance, in the last example, the ampere-turns per centimetre for a flux-density of 21,900 in solid iron are 590 (see Fig. 47). The ampere-turns on the teeth are

$$4\times590=2360.$$

Where very high densities are employed it is convenient to have curves which gives us directly the ampere-turns per inch or per cm. for any apparent flux-density and any given K_s, such curves are given in Fig. 47.

The method of calculation given in the last example would lead to inaccurate results if applied to those cases in which the teeth are very much tapered and fairly highly saturated, because a small increase in the flux-density at the root of the tooth may call for a very great increase in the ampere-turns per centimetre. Some designers take several sections of the teeth at different distances from the root and calculate separately the ampere-turns necessary to drive the flux along each portion of the tooth.

A method* which is less tedious than this, and at the same time more accurate, is the one employing the curves in Fig. 49, which show how the values of $\int \text{H}\,d\text{B}$ change with the values of B for different values of K_s. The method is founded upon the following theory:

Imagine a very long taper tooth (Fig. 48). Fix a datum mark DD, from which to measure lengths along the tooth, such as l_1 and l_2. This datum mark may be

* See Hird, *Jour. Institution of Electrical Engineers*, vol. 29, p. 933.

somewhere at the wide end of the tooth, where the flux-density is very low; l_1 and l_2 are lengths measured from the datum line towards the narrow end.

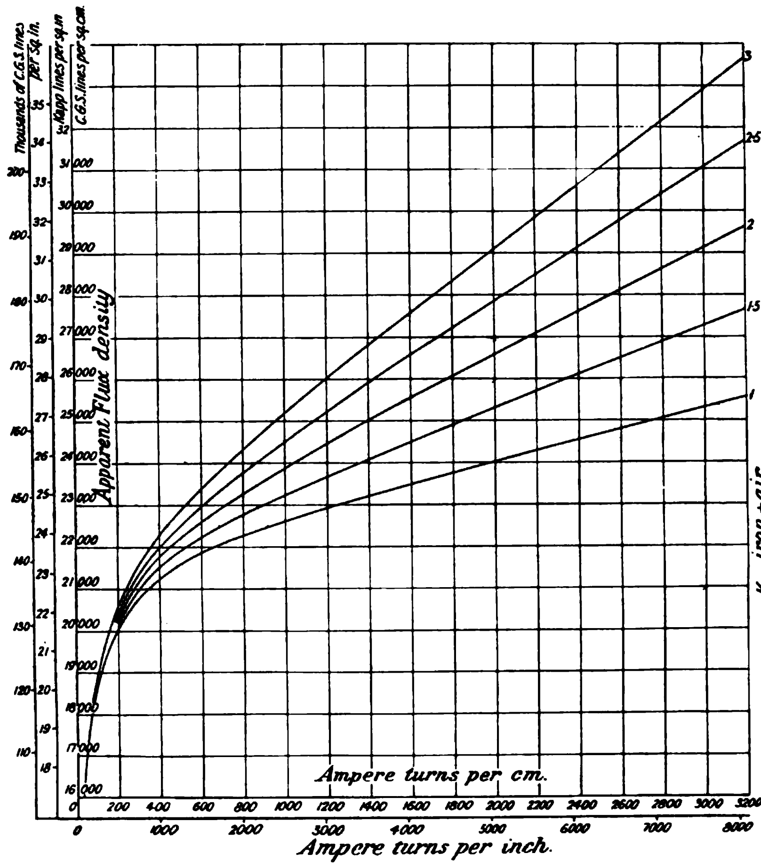

FIG. 47.—Curves giving relation between apparent flux-densities in teeth and the ampere-turns per unit length for different values of K_s.

For small changes in l we may take B as almost following a linear law. This is the more true in practice where K_s increases at the root of the tooth. Thus we can write approximately $B = kl + \text{constant}$, where $k = (B_2 - B_1) \div (l_2 - l_1)$. B_1 and B_2 are the flux-densities at the distances l_1 and l_2 from the datum mark.

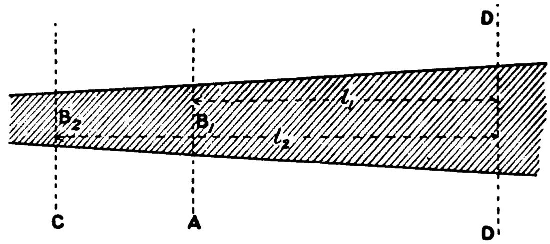

FIG. 48.—Showing convention upon which the rules for dealing with taper teeth are based.

Thus, we have $$d\mathsf{B} = k\,dl.$$

Now the magnetomotive force required to drive the flux from A to C is

$$\int_{l_1}^{l_2} \mathsf{H}\,dl = \frac{1}{k}\int_{\mathsf{B}_1}^{\mathsf{B}_2} \mathsf{H}\,d\mathsf{B}.$$

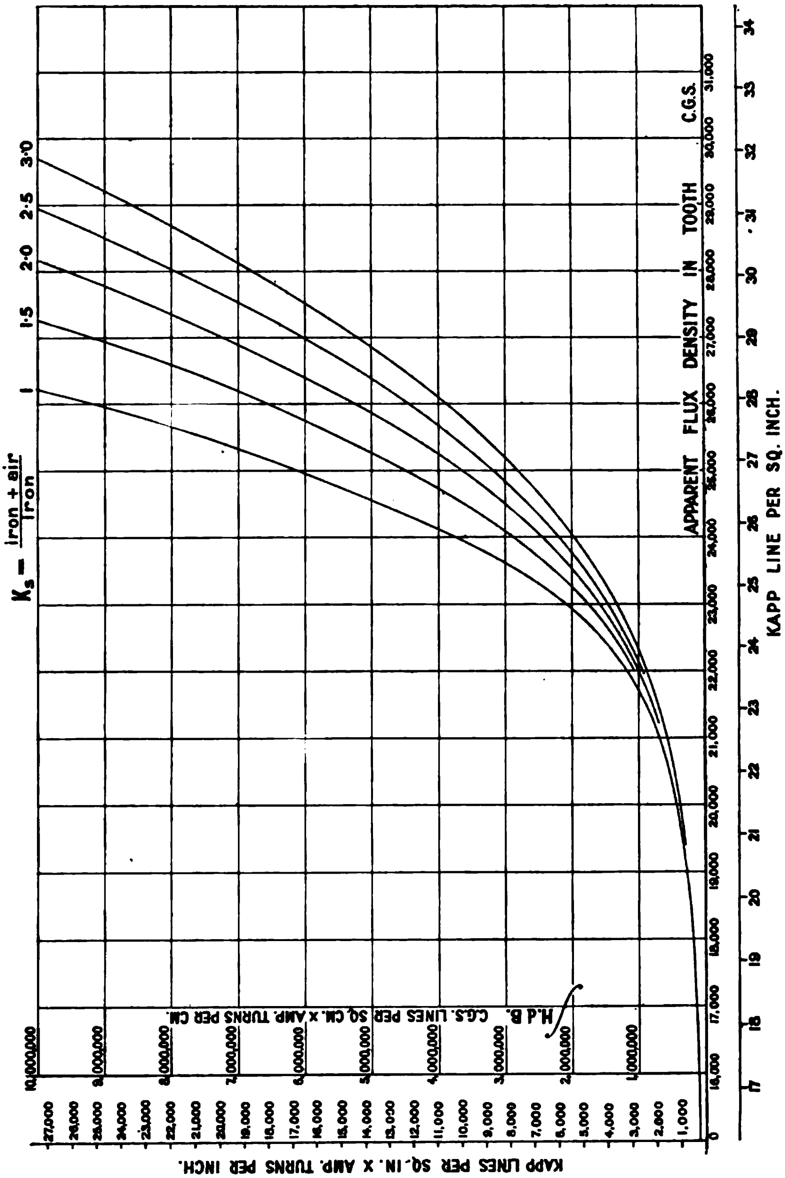

FIG. 49.—Curves for quickly finding the ampere-turns expended on taper teeth.

If, therefore, we have plotted curves of which the ordinates give the values of $\int \mathsf{H}\,d\mathsf{B}$, the abscissae representing B, the value of $\int_{\mathsf{B}_1}^{\mathsf{B}_2} \mathsf{H}\,d\mathsf{B}$ can be immediately

obtained by subtracting the ordinate for B_1 from the ordinate for B_2. And the ampere-turns in the tooth will be $\frac{l_2 - l_1}{B_2 - B_1}\int_{B_1}^{B_2} H\,dB$, provided that we have employed suitable units in plotting the curves.

In Fig. 49 we have given the value of $\int H\,dB$ for the cases where the slot and vent spaces form parallel paths with the teeth. Each value of K_s requires a separate curve. It is sufficient to plot curves for the values of K_s given in Fig. 49. The positions of intermediate curves can be judged very well by eye.

The manner in which these curves are employed to find the ampere-turns expended upon the teeth is best seen from an example.

EXAMPLE 13. The armature of a direct-current motor is 36 cms. in diam. and 25 cms. long. It has 37 slots, each 1·1 cms. wide and 3·5 cms. deep. There are 2 ventilating ducts, each 1 cm. wide, and the iron laminations are 91 % solid iron. What number of ampere-turns is required to drive the flux along the teeth and slots when the total $A_g B = 26$ megalines (see page 7)?

First find the cross-section of all the teeth on the tops.

$$36\pi = 113$$
$$37 \times 1{\cdot}1 = \underline{40{\cdot}7}$$
$$72{\cdot}3 \times (25 - 2) = 1660 \times 0{\cdot}91 = 1510 \text{ sq. cm. of solid iron in tops of the teeth.}$$

Apparent flux-density $\frac{26 \times 10^6}{1510} = 17{,}220 = B_1$,

$$113 \times 25 = 2820 \text{ sq. cms. of air and iron,}$$
$$K_s \text{ for tops of teeth} = \frac{2820}{1510} = 1{\cdot}87.$$

Next take the roots of the teeth

$$(36 - 7)\pi = 91$$
$$37 \times 1{\cdot}1 = \underline{40{\cdot}7}$$
$$50{\cdot}3 \times (25 - 2) = 1160 \times 0{\cdot}91 = 1055 \text{ sq. cms. of solid iron in roots of the tooth.}$$

Apparent flux-density $\frac{26 \times 10^6}{1055} = 24{,}650 = B_2$,

$$91 \times 25 = 2280,$$
$$K_s \text{ for roots of teeth} = \frac{2280}{1055} = 2{\cdot}16.$$

Referring now to Fig. 49 and taking the curve for $K_s = 2$.

For $B_2 = 24650$ the ordinate $= 3{\cdot}2 \times 10^6$.

For $B_1 = 17220$,, $= 0{\cdot}15 \times 10^6$

$\ 7430 \qquad 3{\cdot}05 \times 10^6$

$$3{\cdot}05 \times 10^6 \times \frac{3{\cdot}5}{7430} = 1440 \text{ ampere-turns on the teeth.}$$

The quantity $l_2 - l_1$ is of course the length of the tooth, in this case 3·5 cms.

It should be noted that in this book we consider the ampere-turns on one pole, not the ampere-turns per pair of poles as is sometimes done.

Air-gap and tooth saturation curve. The name "saturation curve" is sometimes given to a curve which shows the relation between the voltage generated by a machine and the exciting current, or to a curve which shows

the relation between the flux per pole and the ampere-turns on the pole. Curves of this kind are given later (see pages 365 and 398). At this stage we wish to consider another kind of saturation curve, namely, one showing the relation between the flux-density in the air-gap and the ampere-turns on the gap and teeth. Such a curve is of the greatest service in all investigations of the flux distribution under a pole on no load and on full load.

It will be seen, in the first place, that for a certain armature punching, built up with a certain solidity and with a certain number of ventilating ducts, there will always be certain relation between the flux-density in the air-gap and the saturation of the teeth near the region in the air-gap, where the flux-density is under consideration, and this is independent of the number of poles or the state of the load. Thus, a certain number of ampere-turns will always be required for gap and teeth (taken together) for a certain flux-density in the gap. In what is said here we are of course neglecting the irregularity in the flux-density produced in the immediate vicinity of a tooth considered by itself, by the presence of an open slot or any such very local disturbance. That disturbance is allowed for in the contraction ratio, but otherwise is neglected. By flux-density in the gap we mean the average flux-density over the pitch of one tooth.

In plotting a gap and tooth saturation curve it is convenient to compare all flux-densities to the density at a point in the air-gap mid-way between armature and field-magnet. If we start with the quantity $A_g\mathrm{B}$ (see p. 7), and divide this by A_g, the area of the active surface of the armature, this surface should be taken to be the cylindrical surface lying mid-way between the armature and the field-magnet. Thus, with a rotating armature

$$A_g = \pi(d_a + g) \times l_a.$$

The apparent flux-density in the teeth at any distance from their roots is obtained by dividing $A_g\mathrm{B}$ by the total area of all the teeth at that distance from the roots.

To plot our curve, then, we want in the first place A_g as a standard of reference for all other areas through which the flux has to pass.

Now take $\mathrm{B} = 10{,}000$ (or $\mathrm{B}_K = 10$ kapp lines per square inch if we prefer those units), and calculate the ampere-turns on the gap, making allowance for the contraction ratio as was done on page 65.

On a piece of squared paper lay out ampere-turns per pole as abscissae, and flux-density in the gap as ordinates. As the ampere-turns on the gap are strictly proportional to the flux-density in the gap, a straight line joining the point giving the ampere-turns on the gap for $\mathrm{B} = 10{,}000$ with the origin will give the ampere-turns on the gap for any flux-density. It is known as the air-gap line. Now take several values of B in the gap, say, 8000, 9000, 10,000, and 11,000. For each of these values divide $A_g\mathrm{B}$ by the section of all the teeth, and calculate the ampere-turns required for the teeth for each value. Lay off on the paper those additional ampere-turns as additions to the abscissa for the ampere-turns on the gap for each value of B in the gap. This gives us the curve we want.

EXAMPLE 14. Take the data given in Example 11, page 71. Assume that the radial length of the air-gap is ¼″, and plot the gap and tooth saturation curve.

To get the ampere-turns on the gap we must first take the contraction ratio. Now, $s=0\cdot5$ and $g=0\cdot25$, $\frac{s}{g}=2$. Pitch of slots is $1\cdot16=p_s$, so that $\frac{s}{p_s}=0\cdot43$. From Fig. 36 the contraction ratio due to slots is $1\cdot135$.

There are 4 vents, each $\frac{3}{8}''$ wide, total $1\cdot5''$ The armature is $13''$ long, so that $\frac{s}{p_s}$ for the ducts is $\frac{1\cdot5}{13}=0\cdot115$, and $\frac{s}{g}$ for the ducts $=\frac{0\cdot375}{0\cdot25}=1\cdot5$. K_g for the ducts $=1\cdot03$. The total contraction ratio is therefore $1\cdot135\times1\cdot03=1\cdot17$.

For $B''=60{,}000$ lines per sq. in.

Ampere-turns on the gap $=0\cdot313\times B''\times g''\times K_g$

$$=0\cdot313\times60{,}000\times0\cdot25\times1\cdot17=5500.$$

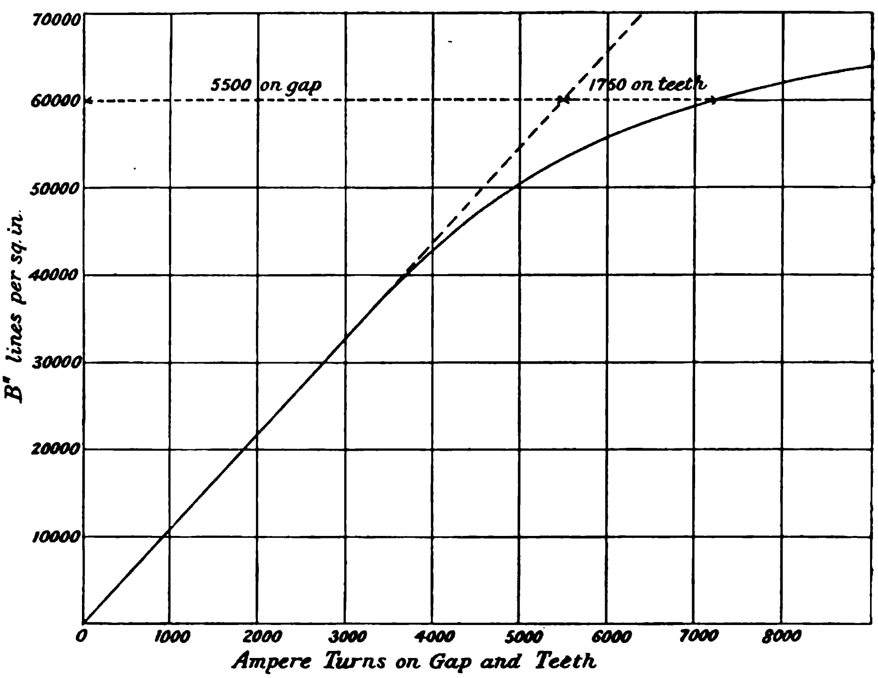

FIG. 50.—Illustrating the method of constructing an air-gap-and-tooth-saturation curve.

Mark on squared paper the point $B''=60{,}000$, $K_g=5500$, and join the point to the origin. This gives us the air-gap line.

As the teeth are short as compared with the diameter of the armature, it is sufficiently accurate to take the area of the teeth as we did on page 71.

$$A_t=\{(d-1\cdot5l_t)\pi-(\text{No. of slots}\times\text{width})\}\times\text{net length of iron.}$$

In this case $A_t=2370$ sq. in. of laminations, or 2110 sq. in. of solid iron.

Now $$A_g=(120+0\cdot25)\pi\times13=4900 \text{ sq. in.,}$$

$$K_t=\frac{2110}{4900}=0\cdot43.$$

To get the apparent flux-density in the teeth for any flux-density in the gap we merely divide by K_t, and to get the actual flux-density we can refer to Fig. 46. As we know K_s (in this case 2·28, see page 71), we can refer at once to Fig. 47 and read off the ampere-turns per inch required for the teeth.

Make four columns:

B″ in gap.	B″ app. in teeth $=B''_g \div 0{\cdot}43$.	Ampere-turns per inch (Fig 47).	Ampere-turns in teeth.
45,000	105,000	60	120
50,000	116,000	180	360
55,000	128,000	400	800
60,000	139,000	850	1750
65,000	151,000	1900	3800

It is hardly necessary to remind a technical student that to get column 2 from column 1 it is only necessary to put 4·3 on the C scale of a slide rule opposite 1, and then read off column 2 from scale D.

The higher values of the ampere-turns per inch can be taken from Fig. 47. The lower values can be more accurately taken from Fig. 21. The additional ampere-turns required for the teeth are plotted as in Fig. 50. Other examples of curves of this kind will be found in Figs. 301 and 373.

The considerations to be kept in view in designing the tips of teeth on armature cores. These are as follows:

(1) The object of the tip is to make the head of the tooth as wide as possible without increasing unduly the inductance of the conductors in the slot.

(2) Sufficient iron must be provided at the root of the tip to carry the flux passing through the tip, and to give it mechanical strength.

(3) The permeance of the magnetic path encircling the slot (that is the path for leakage lines) is to be kept as low as possible.

(4) The slot should be of such a shape as to only require a simple die to punch it, and the corners should be such that they will punch well without requiring the frequent repairing of the die.

(5) The mouth of the slot must sometimes be not less than a certain minimum, as when it is intended for mush coil winding.

(6) The method of drawing the slot and making the punch for it should be capable of being easily standardized.

The following names of parts and symbols will be used (Fig. 51 illustrates the parts):

h = height of slot.	m = mouth of slot.
h_c = height of conductors.	p = lip of mouth.
b = width of slot.	g = air-gap.
r = root of tip.	α = angle of slope of tip.

As to the general shape of the tip, it is only in special cases that anything is to be gained by the use of a large radius at the corner of the root, as in

Fig. 52. For a standard slot, of normal size (b from 0·25 to 1 inch, h from 0·5 to 2 inches), intended to take various numbers of conductors, and various amounts of insulation, the shape of slot (Fig. 51) is as good as any other. In drawing it, the corners may be shown sharp, and the die maker will put on a very small radius to get good results in punching. He can make a cheaper tool than if he has to work to special radii which change with every slot.

In general, the best value for the angle a is about 27 degrees, that is, $\tan^{-1} 0{\cdot}5$. This angle gives sufficient iron in the root of the tip both for the working flux and the leakage flux, when the flux-density in the gap is as high as 60,000 lines per sq. inch. The angle might in some cases be reduced where it is very desirable to save space, but in general the same effect can be obtained by drawing the sloping line a little lower down, while still keeping it at the same slope as shown in Fig. 53. For slots of the most ordinary size the apex of the angle a may lie on the centre line of the slot, as shown in Fig. 53.

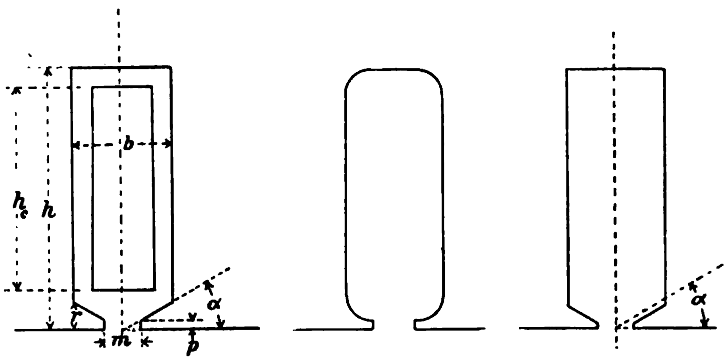

FIG. 51. FIG. 52. FIG. 53.

The dimension m may be fixed by the necessity of putting wires of a certain size through the mouth of the slot. In any case it should not be made too small (say not less than 0·05 inch), on account of the necessity of giving sufficient strength to the metal of the punch. Subject to these considerations m will be made as small as possible, so that the face of the tooth may be as large as possible, due regard being had to the effect of the shape of the tip on the permeance of the path for magnetic lines immediately encircling the slot.

It is useful to have a diagram like that given in Fig. 54, which gives at a glance the values of permeance of the magnetic path across the mouth of the slot for different shapes of tips. This diagram is used in the following way:

Suppose that the mouth of the slot, m, is to be 0·35 of the width, b. At the point 0·35 on the horizontal scale erect a perpendicular as shown in the figure. If it has been decided provisionally that the apex of the angle a shall lie on the centre line of the slot, this perpendicular is drawn to meet the line OA, and the shaded area gives us a picture of the tip. As the perpendicular is always one half of the abscissa, we can judge at once whether the dimension p is or is not too small to punch well. Now carry up the perpendicular (as shown by the dotted line) until it cuts the curve A', and the ordinate gives us

the value of the permeance of the path across the mouth of the slot for one centimetre length of iron, independently of the size of the slot. For instance, in the case taken in the figure there would be 0·98 C.G.S. lines across the mouth of the slot, for every cm. of length of slot, for every $\frac{10}{4\pi}$ amp. carried by the slot. In order to get the total permeance of the slot, this value must be added to the permeance of the path between the parallel sides. If for any reason it is desirable to lower the sloping line, as in Fig. 53, then a line such as *B* in Fig. 54 will be the boundary line of the slot, and the curve *B'* gives the permeance of the path across the mouth of the slot. If, as in turbo-machines,

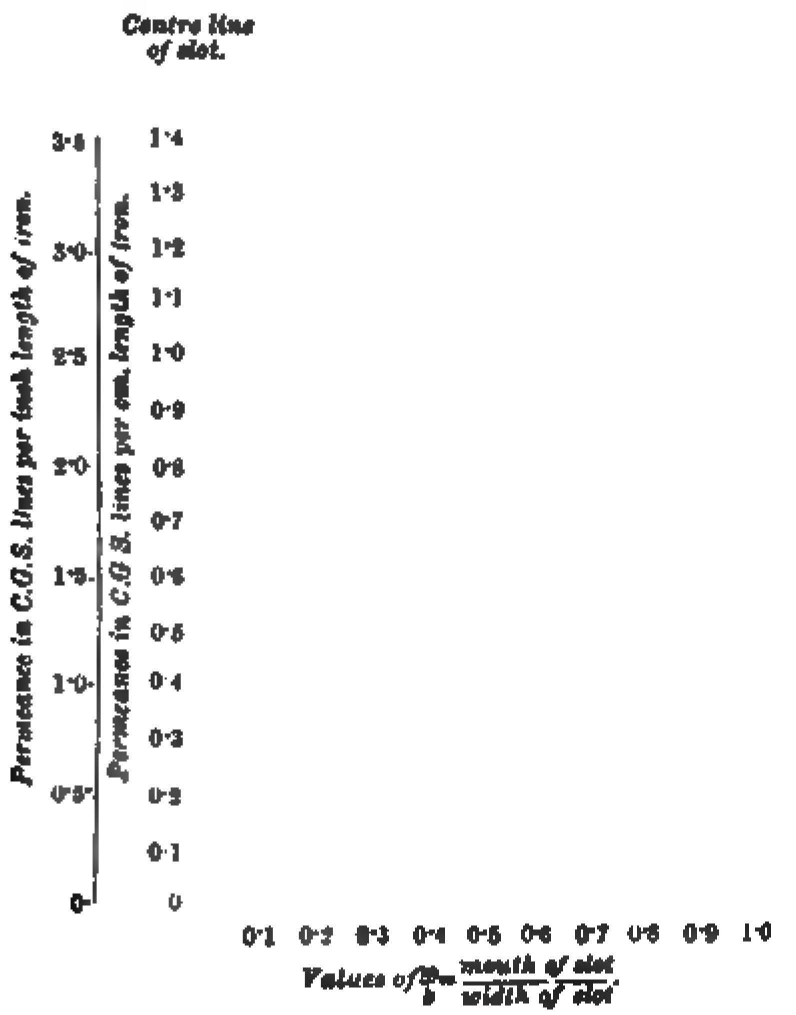

FIG. 54.—Curves for quickly calculating the permeance of the path across the mouth of a slot.

it is necessary to make the tip thicker at the root, the sloping line may be taken in a position such as *DC*, then the permeance is given by the curve *C*. For any intermediate size or shape of slot it is easy by eye to interpolate the point on an imaginary curve, say between *A* and *B*, which gives the value of the permeance of the path across the mouth of the slot.*

For instance, to calculate the leakage flux per centimetre of axial length of iron for the slot shown in Fig. 51 when 200 amperes are flowing in the conductor, we proceed as follows: The leakage across the body of the slot for one ampere in the conductor is

$$\frac{4\pi}{10} \times \frac{1}{3} \times \frac{h_c}{b} = \cdot 419 \times \frac{2\cdot 7}{1\cdot 3} = 0\cdot 87.$$

* These curves may be regarded as supplementary to those given by Dr. Goldschmidt, p. 353, vol. lx., of *The Electrician*, in his excellent article on the "Leakage of Induction Motors."

The leakage across the mouth of the slot when

$$\frac{m}{l}=\frac{\cdot45}{1\cdot3}=\cdot35 \text{ is } 0\cdot89 \quad \text{(from Fig. 54).}$$

The total leakage for one centimetre axial length of iron for 200 amperes is

$$(0\cdot87+0\cdot89)200=375 \text{ C.G.S. lines.}$$

When we are dealing with an alternating current we must remember to take its maximum value if we want the maximum value of the leakage.

For examples of the calculation of the leakage across slots, see pages 422 and 463.

Flux-densities in the teeth. In slow-speed continuous-current machines, where the frequency is low (15 to 25 cycles) very high flux-densities in the teeth can be employed. There is an advantage in employing high flux-densities in such cases, as the commutation is improved thereby, and it will be seen from Fig. 29 that at low frequencies very high saturations can be employed without danger of overheating. Flux-densities as high as 21,000 C.G.S. lines per sq. cm. can be employed with advantage on such machines, and, allowing for the amount of flux that finds its way through the slot space and ventilating ducts, the apparent flux-density may be as high as 28,000 C.G.S. lines per sq. cm. (see Figs. 45, 46 and 47).

Similarly, in 25-cycle A.C. generators and induction motors very high flux-densities are often employed in the teeth. A density of 22,000 is not uncommon in such cases, but the cooling conditions of each case must be studied to see if such densities are permissible (see page 324). In the case of induction motors the density is often limited by the prescribed limit to the magnetizing current. This is especially so on machines having a small pole pitch (see pages 419 and 446).

In 50-cycle machines it is generally necessary to reduce the flux-density so that the losses on the teeth may not be so excessive as to interfere with the cooling of the coils. A density of from 18,000 to 20,000 may be taken as fairly high for 50-cycle machines. Each case must be considered with regard to the cooling conditions (see page 470) and effect on the efficiency.

The iron behind the slots. For the types of machines ordinarily manufactured it will be found that it is not worth while to calculate accurately the number of ampere-turns required to drive the flux through the **armature core** (or **the iron behind the slots**, as it is sometimes called). The reason is that in most cases these ampere-turns are small compared with the total ampere-turns on the pole, so that an inaccuracy of 50 per cent. will hardly affect the total. As the flux distributes itself in some such manner as indicated* in Fig. 58, it would be necessary to find $\int H\,dl$ all along l_a (see Fig. 31) in order to find the ampere-turns correctly. As this is too much trouble in practical calculation, one adopts the following rule, which, though far from giving an accurate result, is good enough when one considers the uncertainties that enter into more important parts of the calculation of a machine. Find the maximum flux-density in the iron behind the slots by dividing the working flux per pole by twice the cross-section of the iron

* See Dr. W. M. Thornton, "The Distribution of Magnetic Induction and Hysteresis Loss in Armatures," *Jour. Inst. Elec. Engrs.*, vol. 37, page 125.

behind the slots. The ampere-turns per pole required to drive the flux through the core will in general be found to be rather less than the ampere-turns required to create this flux-density in an iron path, whose length is equal to one-third of the pole pitch. So, if we find the number of ampere-turns per centimetre required for the maximum flux-density in the core, and multiply by one-third of the pole pitch in centimetres, this gives us a safe figure for ampere-turns.

FIG. 55.—Distribution of flux in the iron behind the teeth.

EXAMPLE 15. In the 1500 H.P. motor worked out in Chapter XVII., the flux per pole is $5{\cdot}6 \times 10^6$ C.G.S. lines. The section of iron behind the slots is 332 sq. cms. The flux-density is therefore

$$5{\cdot}6 \times 10^6 \div 664 = 8450 = \mathfrak{B}.$$

This will require about 3 ampere-turns per cm., and as the pole pitch is 31·2 cms., the ampere-turns per pole required for the core are about 32. As the total ampere-turns per pole are over 1400, it will be seen that it would be useless to make a more careful estimate.

In small armatures less than 36 inches in diameter, the punchings are usually made in one piece, so that the iron behind the slots forms an unbroken magnetic path of very low reluctance. These unbroken cores will carry a greater flux for a given iron loss than cores built up of interleaved segments, having many breaks in the circumference. The breaks in the continuity of the iron bring about losses, not so much at the breaks themselves, as in the surrounding parts of the iron core and frame, owing to the reluctance of the break. If the mean flux-density in the core is as high as 12,000 or 13,000 lines per sq. cm., we will find that at the break-joints part of the flux only keeps to the iron path, and some crosses the small air-gap between the abutting ends of the broken punchings. The amount of the flux which crosses this air-gap is easily calculated from Fig. 46. Let the mean flux-density in the core be 13,000. Then, assuming that one-half the punchings bridge the break-joint, the apparent flux-density in the iron will be 26,000. From the curve $K_s = 2$ in Fig. 46 we find that the actual flux-density is only 23,700, so that the density in the air-gap will be 2300. If, now, the distance between the abutting ends of the punchings is 0·02 inch, or, say, 0·05 cm., the ampere-turns required for the break-joint will be $2300 \times 0{\cdot}05 \times 0{\cdot}795 = 91$.

Now, if, as depicted in Fig. 28, the small air-gap between the breaks is not uniform for the whole width of the core, there will be a tendency for the flux to

crowd to the side of the machine where the air-gap is smallest, and it will be seen that 91 ampere-turns, or even half of it, is sufficient to produce a considerable difference in the distribution of the flux.

Even when this small air-gap is uniform, there is a tendency for some of the flux to be driven out into the iron frame or end plates, but very little loss can occur from eddy currents due to this cause, unless the gap is too big or the flux density in the core excessive. It is important in machines of low frequency, where the core densities are made very high (14,000 to 15,000) to see that the joints are well made. In cases where the frame is split and all the punchings are cut through, the length of air-gap between the punchings is of vital importance. The surface of the joint should be carefully finished off, so that the gap is reduced to at most a few thousandths of an inch, otherwise the flux will be driven into the frame and considerable heating occur around the joint. In four-pole machines a complete break in the punchings on a horizontal or vertical diameter is almost sure to produce an eddy current in the shaft. Eddy currents in the shaft can also be caused by dissymmetries in the break-joints of an armature, even when there is no complete break in the punchings.

For a frequency of 50 or higher the flux-density in the iron behind the slots is generally limited by considerations of iron loss. For low frequencies (15 cycles or lower) the reluctance of the path is the more important consideration. We will take first the higher frequency cases. The amount of iron loss permissible in a core depends upon the facility with which the heat can be dissipated. If the ventilating ducts are very near together, and there is a good draught, one can work the core at a higher density than when the ducts are further apart and the draught not so good. In 50-cycle machines with the natural ventilation obtained from an ordinary speed, and having $\frac{3}{8}$ inch ventilating ducts spaced with a pitch of 2·5 inches, one can work safely at a core density of 11,000 lines per sq. cm. This gives, according to Fig. 29, a loss of about 1 watt per cubic inch, and in a core 4 inches in depth the cooling surface comes out at about 1·2 sq. in. per watt (for core loss only), counting both sides of the ventilating duct and the surface at the back of the iron. When the frequency is higher, it is usual to increase the number of ventilating ducts so as to be able to work at a higher loss per cubic inch ; and at the same time the flux-density is diminished so as to keep the loss per cubic inch within reasonable bounds. At 100 cycles, for instance, one might, with ordinary cooling conditions and ordinary iron, work the core at $B=8000$, giving a loss of, say, 1·5 watts per cubic inch. The ventilating ducts might then be spaced with a pitch of 1·5 inches. The depth of iron in high-frequency machines is for the same speed smaller than in low-frequency machines, so that the cooling conditions are better. In turbo-generators with forced draught, the permissible density in the core requires special study (see page 391). In low-frequency machines it is not advisable to increase the flux-density much above 16 kapp lines or $B=15{,}000$, because at about that point the ampere-turns per inch increase very quickly.

The flux per pole is calculated by dividing the quantity $A_g B$ by the number of poles and multiplying the firm constant K_f. As the flux from the pole divides into two, one-half going to the pole on the right and the other to the pole on the left, we must divide the total flux by twice the area of the core to obtain the mean flux-density.

EXAMPLE 16. A direct-current generator has eight poles, with a flux form constant of 0·7. The diameter of the armature is 92 cms., and the net length of iron 28 cms. There are 96 conductors in series, and at a speed of 375 R.P.M. it generates 255 volts. What must be the depth of iron below slots in order that the density in the core shall not exceed 12,000 lines per sq. cm.?

From formula (1), page 24, we have

$$255 = 0{\cdot}7 \times \frac{375}{60} \times 96 \times A_g\mathsf{B} \times 10^{-8}.$$

$$A_g\mathsf{B} = 60{\cdot}7 \text{ megalines.}$$

$$\text{Flux per pole} = \frac{60{\cdot}7 \times 0{\cdot}7}{8 \text{ poles}} = 5{\cdot}32 \text{ megalines.}$$

$$\frac{5320000}{2 \times 12000} = 221 \text{ sq. cms.} = \text{cross-section of core.}$$

As the net length is 28 cms. the depth below is $\frac{221}{28} = 7{\cdot}9$ cms.

EXAMPLE 17. A certain 4-pole A.C. turbo-generator frame has a punching whose depth behind slots is $8\frac{3}{4}''$, and the net length of iron 40″. How many conductors must we have for a 3-phase star-wound generator running at 1500 R.P.M., in order that the flux-density in the core shall not exceed 11 kapp lines per sq. in.?

$$8\tfrac{3}{4} \times 40 = 350 \text{ sq. in. of core,}$$

$$350 \times 2 \times 11 = 7700 \text{ kapp lines per pole.}$$

Take the form constant, K_f, at 0·64 and the volt constant K_e at 0·4.

$$A_g\mathsf{B}_K = \frac{7700}{0{\cdot}64} \times 4 \text{ poles} = 48{,}000 \text{ kapp lines,}$$

$$6600 \times 10^6 = 0{\cdot}4 \times 1500 \times Z_a \times 48{,}000,$$

$$Z_a = 229.$$

As 229 conductors would not be suitable for a 3-phase generator, we might, if there were 48 slots, make 240. The density in the iron behind the slots would be 10·5 kapp lines per sq. in.

Ampere-turns on the yoke. In machines like continuous-current generators, having external field magnets, the length of path through the yoke is often quite considerable, and the number of ampere-turns required for this part of the magnetic circuit should be calculated with some accuracy.

The flux carried by the yoke includes the leakage flux as well as the working flux, so that before a calculation of the ampere-turns can be made it is necessary to calculate the amount of leakage. The graphical method of calculating the leakage given on page 326 will be found to be very short and sufficiently accurate for practical purposes. It has the advantage over the method employing formulae, in the fact that it can be so easily adapted to varying shapes of pole. Moreover, the designer, having a picture of the flux distribution before him, can more easily check the result, and he can see what feature in the arrangement of the pole is mainly responsible for the leakage.

Having determined the amount of leakage flux, this is added to the working flux and the whole divided by twice the area of the yoke to get the flux-density.

EXAMPLE 18. On a certain continuous-current generator the working flux amounts to $10{\cdot}5 \times 10^6$ lines per pole and the leakage at full load amounts to $2{\cdot}1 \times 10^6$ lines. If the arrangement of the yoke is as shown in Fig. 432, the area being 650 sq. cms., find the number of ampere-turns required for the cast-steel yoke.

Total flux $=12{\cdot}6\times10^6$ lines. Divide by 2 and by 650, and we get 9700 C.G.S. lines per sq. cm. Referring now to Fig. 22, we find that this requires about 9 ampere-turns per cm., and the effective length of yoke being 33 cms., we find the ampere-turns on the yoke to be about 300.

In the machine to which the above example refers, the cross-section of steel has been fixed more by regard to the stiffness of the yoke than by magnetic considerations, and the flux-density is much lower than one would find in generators of smaller diameter. In a cast-steel yoke reasonably free from blow-holes one may economically employ a flux-density as high as 12,500, and this usually requires about 15 ampere-turns per cm. It is not good practice to carry up the saturation much higher than this, because cast-steel is liable to have blow-holes in it, which may cause unequal pole strengths in a multipolar frame, or may call for an excess magnetizing current if the saturation is carried too far.

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"Iron Losses Induction in Motors due to Flux Pulsations," Bragstad and Fränkel, *Elektrot. Zeit.*, 29, pp. 1074, 1102; 1908.

"Experimental Determination of the Hysteretic Constant," N. Stahl, *Elec. World*, 52, p. 1122, 1908.

"Best Thickness for Iron Sheets in Electrical Work," Loppé, *Ind. Elect.*, 18, p. 413, 1909.

"Dependence of Magnetic Hysteresis upon Wave-form," M. G. Lloyd, *Bureau of Standards*, Bull. 5, p. 381, 1909.

"Testing Iron by the Ballistic Electrodynamometer," Rice and M'Collum, *Phys. Rev.*, 29, p. 132, 1909.

"Magnetic Testing of Iron with Alternating Currents," Campbell, *Inst. Elec. Eng.*, Journ. 43, p. 553, 1909.

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"Hysteresis and Eddy-Current Losses in D.C. Machines," Steels, *Assoc. Ing. Él. Liège*, Bull. 9, p. 341, 1909.

"Comparison of Iron Losses during Alternating, Rotating, and Static Magnetisation," Czepek, *Elektrot. u. Maschinenbau*, 28, pp. 325, 351; 1910.

"Iron Losses in a Rotating Field," Hermann, *Elektrot. Zeit.*, 31, p. 363, 1910.

"Iron Losses in a Rotating Field," Hiecke, *Elektrot. u. Maschinenbau*, 28, p. 683, 1910.

"Magnetism of 'Stalloy,'" H. R. Hamley and A. L. Rossiter, *Roy. Soc. Victoria*, Proc. 23, 2, p. 325, 1911.

"Rotor Hysteresis in Polyphase Induction Motors," D. Robertson, *Electrician*, 68, p. 12, 1911.

"Commercial Testing of Iron for Hysteresis Loss," L. T. Robinson, *Amer. I.E.E.*, Proc. 30, p. 825, 1911.

"Source of Extra Iron Losses in Rotating Smooth Ring Armatures," J. Wild, *Zeitschr. Vereines Deutsch. Ing.*, 56, p. 1441, 1912.

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"Dynamo Sheet Steel," *Metall. and Chem. Engin.*, 10, p. 553, 1912.

"Magnetic Properties of Dynamo Iron," De Nolly and Veyret, *Elektrot. Zeit.*, 30, p. 985, 1912.

"Electrolytic Iron for Electrical Machinery," Breslauer, *Elektrot. Zeit.*, 34, pp. 671 and 705, 1913.

"Magnetic Investigation of Sheet-Iron," Zickler, *Elek. u. Maschinenbau*, 31, pp. 737 and 759, 1913.

"Relation between Magnetic Properties of Steel and Temperature," *Rev. de Métallurgie*, 10, p. 146, 1913.

"Mechanical and Magnetic Testing of Steel," R. P. Devries, *Rev. de Métall.*, 10, p. 141, 1913.

CHAPTER VI.

THE ELECTRIC CIRCUITS.

Armature windings. In laying out the armature winding of any dynamo-electric machine, the logical procedure will be as follows:

(1) Lay out the **conductor diagram,** *i.e.* the number of conductors, number of phases, the position of the conductors relatively to the poles, and the direction in which the currents will pass in the conductors at a particular instant. In this scheme we are only concerned with these matters, and not at all with the end connections.

(2) Make a **connector diagram** showing how the ends of the conductors are to be electrically connected in order to carry out the scheme, thus obtaining our **winding diagram.**

(3) Consider the **mechanical design** of the end connectors to see that they clear one another with sufficient spaces between, and are mechanically strong enough.

(4) Consider the **material** of the conductors.

(5) Their **size** and **shape** of cross-section.

(6) The effect of **eddy currents.**

(7) Calculate the **resistance** and the **weight.**

(8) Consider the **heating** and **cooling.**

1 AND 2. THE CONDUCTOR DIAGRAM AND WINDING DIAGRAM.

Single-phase windings. Suppose that we are laying out the winding of a single-phase generator: Lay out—on squared paper for convenience—fine dotted lines to represent the pitch of the poles, as shown in Fig. 100. The lines of the squared paper can conveniently be taken to represent the slot pitch. Draw with thicker lines the conductors in their proposed relation to the poles, and show, by arrow heads, the direction in which the current will pass at some particular instant. In this scheme we settle how many slots shall be wound out of the total possible number of slots in the pole pitch. For example, it is common in a single-phase machine to wind two-thirds of the slots in the pole pitch, though, for reasons to be dealt with when we come more particularly to consider the design of such machines, another fraction may be taken.

In Fig. 100 the pitch of the slot is one-ninth of the pole pitch, and we have six wound slots per pole. The conductor diagram is therefore completely given in Fig. 100. It is well to settle this simple matter first, before we proceed to the

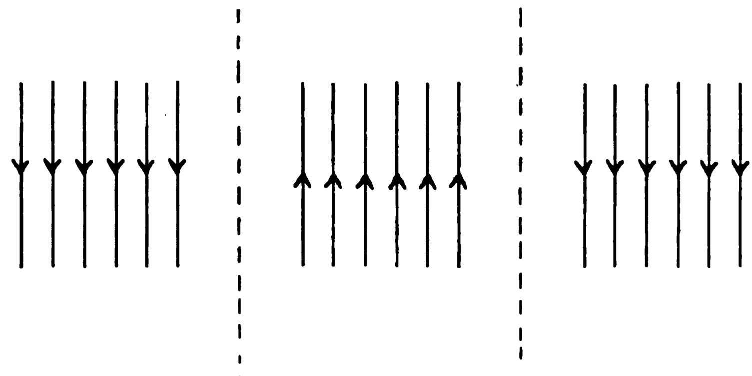


FIG. 100.—Conductor diagram for single-phase winding.

winding diagram, because whatever end connections we may employ to carry out the scheme, they will have no effect upon the amount of the E.M.F. generated, or its wave-form.

We may, for instance, connect any one of the conductors passing under a north pole to any one of the conductors passing under a south pole, and the effect

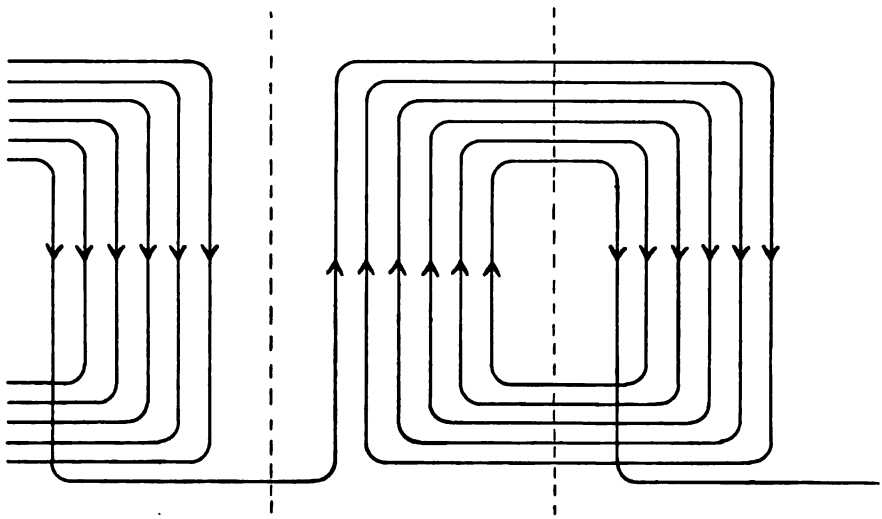


FIG. 101.—Concentric hemitropic connections for single-phase winding.

will be the same, so far as it can be measured at the terminals of the machine. There are, however, certain classes of end connectors which have been found in practice to be the most satisfactory. These will be considered here.

End connections may be broadly classified into concentric connections and

lattice connections. The latter are sometimes spoken of as "overlapping" connections. Figs. 101 and 102 show concentric connections; Figs. 103, 104, 105 and 106 show lattice connections.

It will be seen that these terms "concentric" and "lattice" refer to the type of connections as shown in the "connector diagram." There are a great number of different ways of carrying out *mechanically* each type of connection (see p. 115).

In Fig. 101 all the conductors lying under one pole are connected by means of a broad band of connectors to all the conductors under another pole. This style of winding has been called hemitropic.*

In Fig. 102 the conductors lying under one pole are divided approximately into two parts; half of them are connected to conductors lying under one pole to the right, and half of them to conductors under the pole to the left. This type of

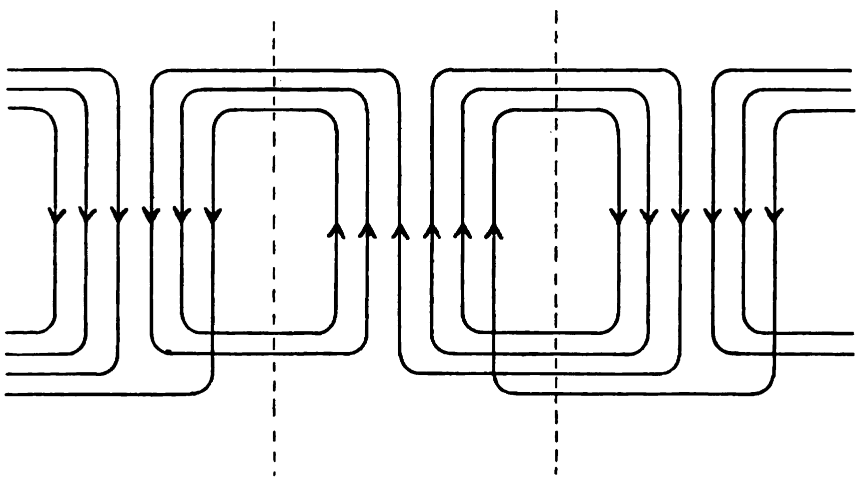


FIG. 102.—Concentric connections for single-phase winding.

winding has the advantage in requiring less copper than the hemitropic winding, as the average length of the end connectors is shorter. It also has the additional advantage in the fact that the armature reaction does not at any instant create a difference of magnetic potential between the iron behind the armature slots and the iron behind the poles, whereas with the winding depicted in Fig. 101 there is, at the instant when the armature current is at its maximum, a very considerable difference of magnetic potential between the armature frame and the field-magnet, which may cause serious eddy currents in the frame or in the shaft. In a three-phase machine this effect is neutralized, because the total current at any instant equals zero.

Where there are a great number of conductors per slot, these conductors will, in general, be grouped in a coil, their end connections being more or less parallel, and they may therefore be considered as forming concentric connections, as between themselves. The coils may then be assembled as concentric coils with the connections between coils made either as in Fig. 101 or Fig. 102, or the coils may be arranged as a lattice work in a manner somewhat similar to Fig. 103.

* See *Polyphase Electric Currents*, by Prof. S. P. Thompson, 1900, p. 85.

Fig. 103 shows a bar winding with lattice connectors, having a throw of a pole pitch at one end and a pole pitch minus one slot at the other. Observe that in this figure the winding is hemitropic, and its magnetic effect will be the same as for the coil shown in Fig. 101. Such a winding should not be employed in single-phase armatures.

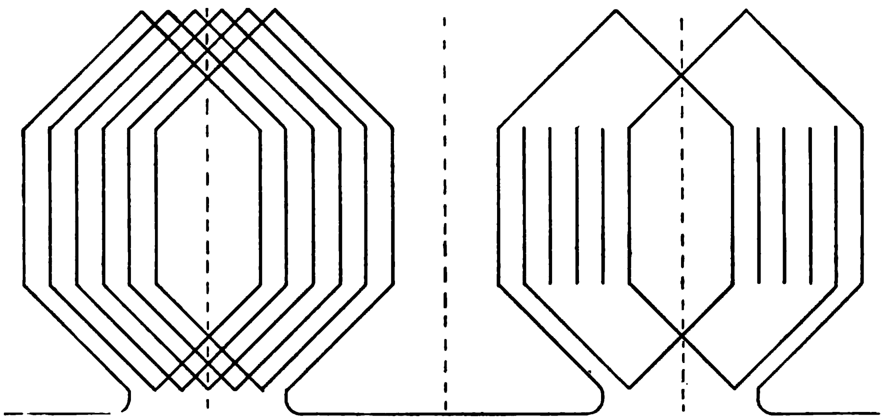


FIG. 103.—Lattice connections, the throw being a full-pole pitch at one end.

In making a diagram of lattice connectors, it is convenient to leave out most of the connectors, as shown to the right of the figure. The diagram, besides being easier to draw, is easier to follow, particularly when several phases are superimposed.

In Fig. 104 is shown a winding of lattice connectors, which in effect are the same as Fig. 102. Here the mean length of connector is less than in Fig. 103, and the magnetic action of the armature is symmetrical.

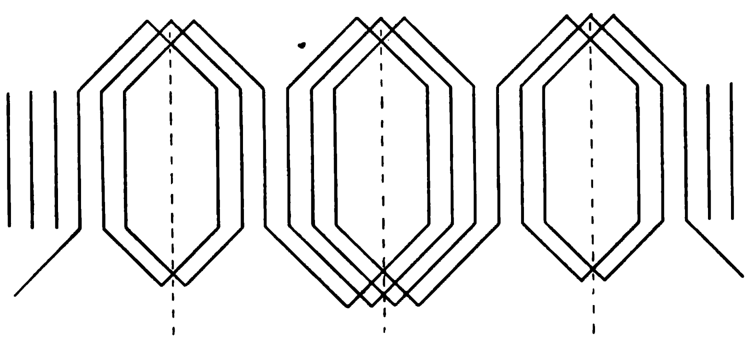


FIG. 104.—Lattice connections with short throw.

All the above connections result in what is sometimes termed a "lap" winding as distinguished from a "wave" winding.

In a wave winding such as shown in Fig. 105, we pass under a north pole, then south, and then the next north, instead of returning under a same north as in the lap winding. Wave windings are very convenient to employ in a bar-

wound machine, because by their use we do away with specially-shaped connectors between one coil and another. It should be remembered here, that with a wave winding the average length of end connector is greater than in the type of winding shown in Fig. 104. In wave windings it is usual to employ two conductors per

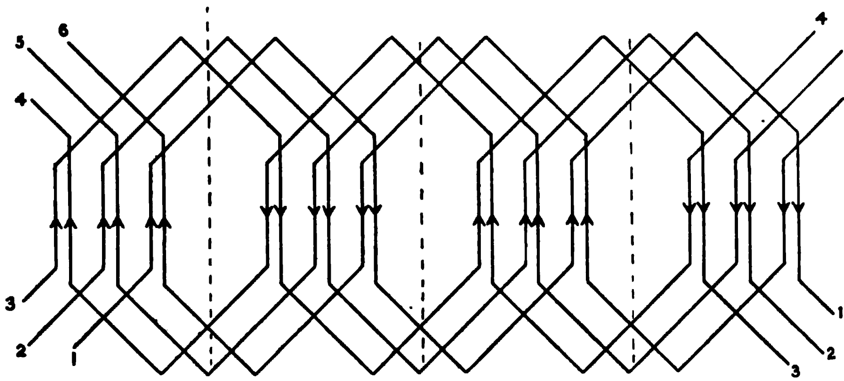


FIG. 105.—Lattice connections forming a wave winding. Six independent circuits closed on themselves.

slot, as this arrangement makes it possible to have a symmetrical arrangement of conductors on the two sides of the machine.

A few years ago it was usual with the simple (two-bar per slot) wave windings to have an odd number of slots, so that after we had progressed around the machine with a number of steps equal to the number of poles, we arrived at a slot

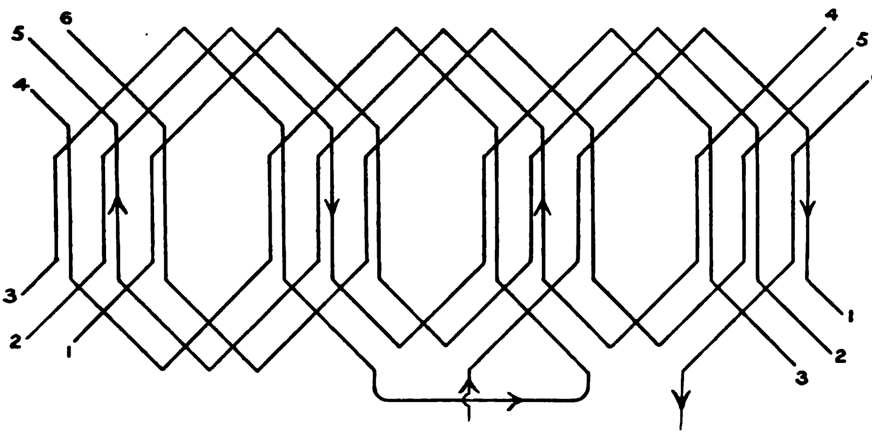


FIG. 106.—Lattice connections forming a wave winding with the throw of six connectors altered so as to put the six circuits of Fig. 105 in series and bring out two ends.

either one short or one ahead of the slot from which we started. Then we stepped round the machine again, coming in either one short or one ahead, and so on until all the slots were filled. This method had the advantage of calling for at most two different lengths of end connectors, and it also had the advantage of changing the phase of the slot very slightly at each throw. Such an arrangement of slots is, however, not always convenient when a standard line of machines must

be laid out. For a standard line, designed to be wound for many different voltages, it is more convenient to have a whole number of slots per pole. In this case the wave winding is just as possible as before, the only difference being that after we have stepped around the machine with a number of throws equal to the number of poles, we make one throw rather shorter or rather longer than before, and come into a slot either one short or one ahead of the one we started from. The number of special connectors required for this method is usually only small, and the difference in pitch is so slight that it is hardly apparent after the machine

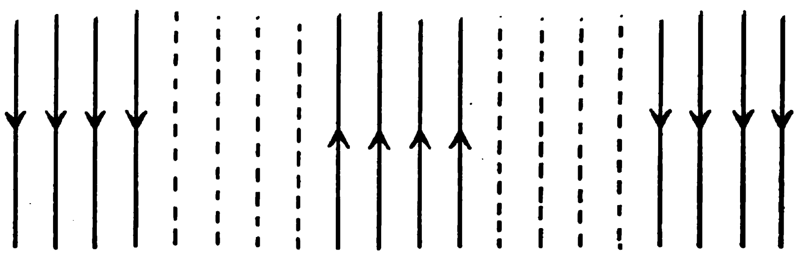


FIG. 107.—Conductor diagram for two-phase winding (full pitch).

has been wound. The easiest way to lay out such a winding as this is to first of all lay out all the connectors as if the throw were constant. We then obtain a number of circuits closed on themselves, as shown in Fig. 105. For convenience in tracing out the winding, we have affixed the numbers 1, 1; 2, 2; 3, 3, etc., to distinguish each closed circuit where it leaves the diagram on the right and where it begins again on the left. We then choose some convenient part of the winding where we wish to put the terminals, and we shorten—or lengthen—some of the connectors, as shown in Fig. 106, in a manner which puts

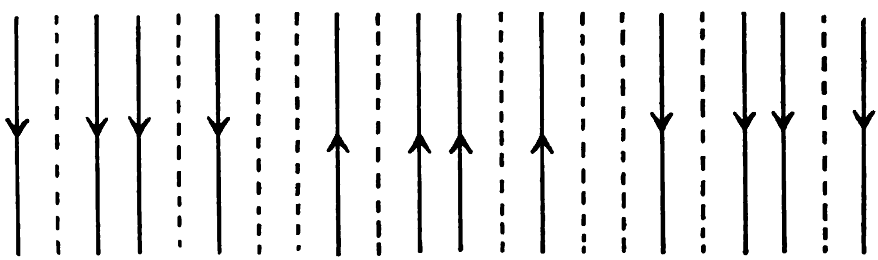


FIG. 108.—Conductor diagram for two-phase winding (short chorded).

all the conductors in series with one another. It will be seen that by the shortening of the throw in the centre of the diagram, circuit No. 1 is put in series with circuit No. 2, and so on.

All these figures (101 to 106) represent different *winding diagrams* for carrying out the *conductor diagram* in Fig. 100.

Two-phase windings. In a two-phase winding, as before, first lay out the *conductor diagram.* Most commonly this will consist of a simple arrangement, such as is shown in Fig. 107. This would be a full pitch two-phase winding. If the winding were chorded, the scheme might appear as in Fig. 108. Fig. 109 shows the arrangement of end connectors for this where there are two conductors per

slot. Other schemes of chording might be employed (see Fig. 120). As stated before, these conductor diagrams do not concern themselves with the end connections, though of course the scheme adopted will affect the length of the end connectors.

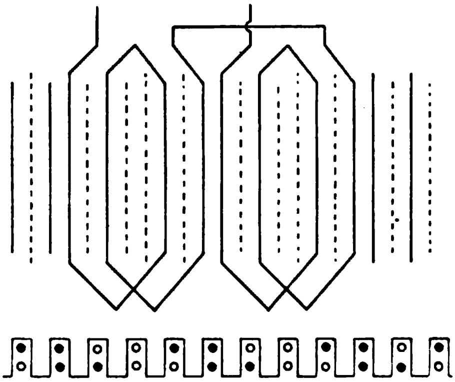


FIG. 109.—Showing two-phase winding after the scheme of Fig. 108 with two bars per slot and connectors having a short throw.

Taking the simple conductor diagram given in Fig. 107, we may connect the conductors of phase A, just as if it were a single-phase machine, by any of the methods illustrated in Figs. 102, 103, 104 or 106. Similarly, we may connect any

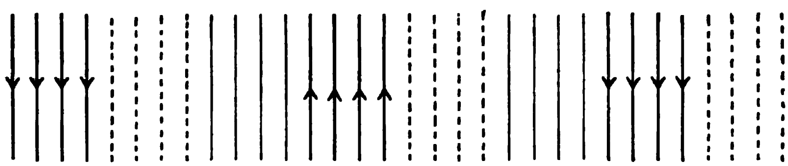


FIG. 110.—Conductor diagram for three-phase winding.

of the conductors in phase B in the same way, but we must remember that the connectors of one phase must keep clear of those of the other.

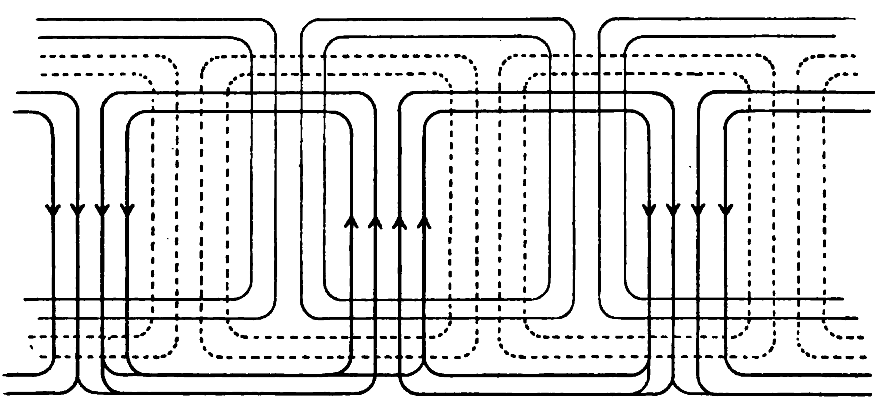


FIG. 111.—Concentric connections on three-phase winding in three tiers.

In two-phase machines a very common method is to arrange the connectors in two tiers, as illustrated in Fig. 113(a). The coils of phase A may have straight ends as shown, and coils of phase B may be bent up so as to clear the projecting bars of A. These are commonly spoken of as "bent ends." If the proportions are approximately as shown in Fig. 113(a), the resistance and self-induction of the two phases will be very nearly alike.

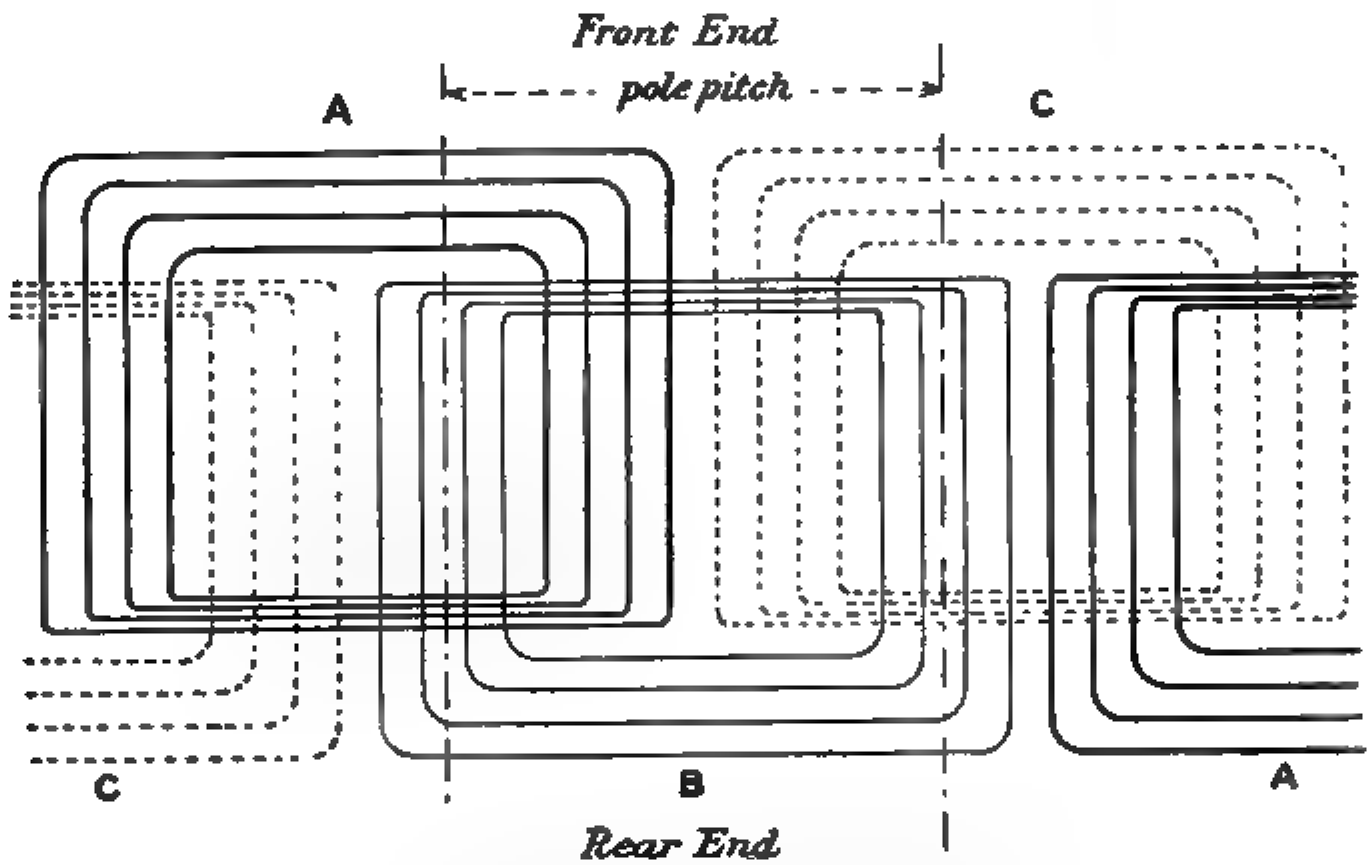


FIG. 112.—Three-phase winding consisting of concentric coils arranged with the ends in two tiers, after the manner illustrated in Fig. 114.

Where lattice connections, such as in Figs. 104 and 105, are employed, the end connectors of one phase lie contiguous to those in another, so that with these types of windings it is necessary to insulate the end connectors to withstand the full pressure between phases.

FIG. 113.—Showing various methods of arranging ends of coils in a two-tier winding.

Three-phase windings. A conductor diagram for a simple three-phase winding is shown in Fig. 110. The most straightforward way of making the end connectors for this is to arrange them in three tiers, as shown in Fig. 111. These three tiers may be arranged in the methods shown in Figs. 142 and 348. Three-tier windings are commonly employed on two-pole and six-pole machines, or where the number

of poles is not a multiple of four. Where the number of poles is a multiple of four it is more convenient to employ the diagram shown in Fig. 112, which enables the connectors to lie in only two tiers, which may be arranged in any of the methods depicted in Fig. 113.

A two-tier winding usually occupies so much less space than a three-tier winding that the diagram shown in Fig. 112 is preferred to the diagram in Fig. 111.

Fig. 114.—Three-phase winding in two tiers, three coils per group.

It will be seen that in the diagram in Fig. 112 one of the groups of coils of phase *A* is long at the front end and short at the rear end. Under the next pole a group of coils of phase *C* is long at the front end and short at the rear end, while the next group of coils of phase *A* is short on the front end and long on the rear end. It will thus be seen that it is only at every fourth pole that the winding repeats itself.

Fig. 114 shows the general appearance of a winding of the type shown diagrammatically in Fig. 112, but with three coils per group.

Where the number of poles is not a multiple of four it is still possible to employ a winding diagram similar to Fig. 113(a) for the greater number of the poles and complete the winding by means of skew coils, as shown in Fig. 115. A skew coil is formed so as to have one half long at the front end and the other half long at the rear end.

FIG. 115.—Skew coils on a ten-pole induction motor.

Figs. 111 and 112 cover the most usual cases of concentric windings. The concentric form of winding is very commonly employed, both on alternating-current generators and induction motors, in those cases in which each coil consists of a number of turns of wire or strap. For bar windings, however, and in some cases even for wire windings, the lattice connector is preferred.

Fig. 116 shows an arrangement of lattice connectors for a winding on a three-phase machine of the hemitropic type. (See also Fig. 103.) In this case the throw of a connector is the full pole pitch on one end and one slot short of a full throw on the other. In Fig. 117 is shown an arrangement of lattice connectors in which the throw is shortened. This corresponds with Fig. 104 of the single-phase case. Although diagrams such as Fig. 116 show only one bar per slot, it is clear that each coil lying in a pair of slots may consist of many turns in series, and the connections between successive coils can be made just as the connections are made from turn to turn in Fig. 104. Lattice coils of this type are illustrated in Fig. 119. In Fig. 134 is shown a winding consisting of lattice-type coils arranged so that each slot contains the limb of only one coil. There are thus twice as many slots as there are coils.

The lattice connector, however, is more commonly used in cases where there are 2 bars per slot or 2 coils per slot. In this case it is convenient to represent the bars by long and short lines on the diagram, each long line representing a bar at the bottom of the slot and each short line representing a bar near the mouth of the slot. The connector must always go from a long line to a short line, as shown in Fig. 118. If there are an integral number of slots per phase per pole, then the connections for any one phase are exactly the same in principle as shown in Figs. 105 and 106, and the terminals would be brought out from each phase in the same way as described with reference to these figures. In order to

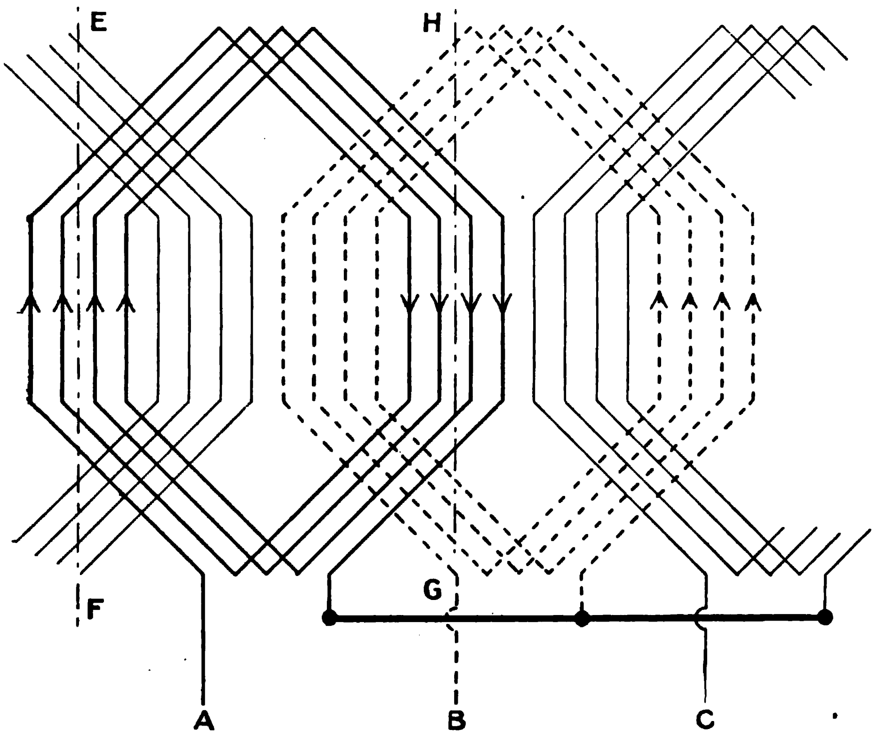


FIG. 116.—Three-phase hemitropic winding with lattice end-connectors.

ascertain which three ends should be brought to the terminals and which three ends should be connected to the star point, it is best to choose one of the phases —say phase C—and draw on the conductors under one pole a large arrow head, indicating the direction in which the current will flow at a particular instant when the current in that phase is at its maximum; a large arrow head pointing in the opposite direction will, of course, be drawn upon the conductors under poles of the opposite polarity. Taking, then, that branch of phase A which lies adjacent to phase C, we will draw a small arrow head pointing in the same direction as the large arrow head of phase C, and on that branch of phase B which lies adjacent to phase B a similar small arrow head will be drawn. These small arrow heads indicate the current of half the maximum value which will be flowing in A and B

at the instant when the current in phase C is at its maximum. This will at once be understood by reference to Fig. 118. Now it is clear that we must make the connections to the star point so that the two half currents from phases A and B run together to form the full current in phase C, and it will be seen that the other

FIG. 117.—Full-pitch, four-pole, three-phase winding, with lattice end-connectors of short throw. The armature of a 4000 K.V.A. turbo-generator, 6000 volts, 50 cycles, 1500 R.P.M. Seventy-two slots, two bars per slot. Connectors arranged as if there were 144 slots with one bar per slot.

three ends can be brought to terminals, the terminal of phase C providing a full current flowing out of the machine, and the terminals of A and B providing half currents flowing in.

Although the end connectors of a wave winding such as shown in Fig. 118 are longer than in the lap windings shown in Figs. 117 and 120, the wave winding

is generally preferred for bar-wound machines, because with it one is able to do away with so many special connectors between groups (see Fig. 120). The commonest method of carrying out the mechanical arrangement of the connectors of a wave-wound machine is shown in Fig. 129. This arrangement is often referred to as a "barrel" winding. It gives a very neat appearance, free from unsightly connections between groups, and has great mechanical strength and good ventilating qualities.

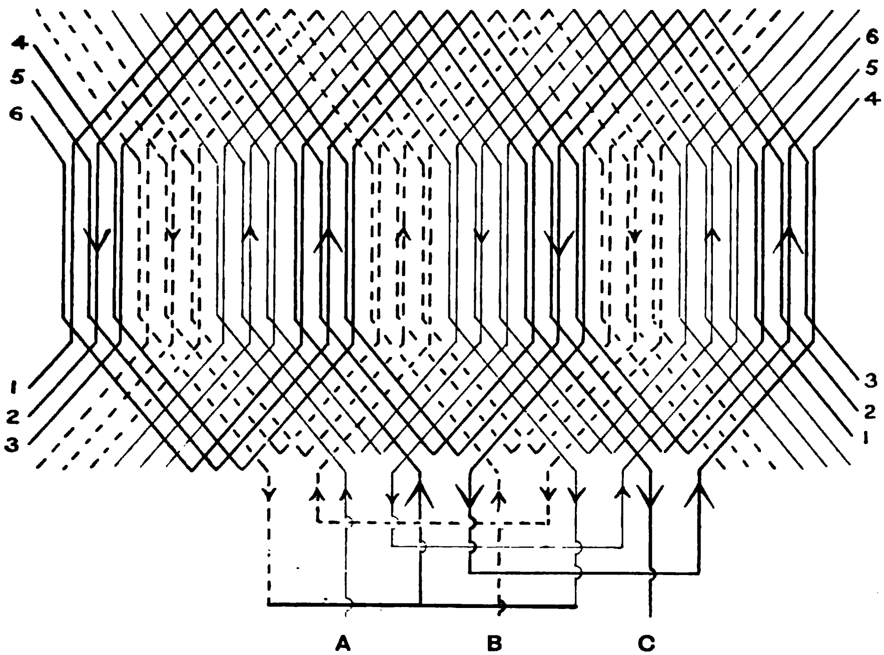


FIG. 118.—Full-pitch, three-phase, wave winding, with lattice connectors. Two bars per slot. The figure shows the method of affixing large and small arrow-heads for the purpose of finding which ends are to be starred and which ends brought to terminals.

As three-phase machines are by far the most common of all alternate-current machines, whether generators or motors, in commercial service, it will be worth while to consider at some length the number of slots which can be conveniently used with any given number of poles.

In the first place (while it is possible to use almost any number of slots by adopting certain artifices), one would usually select a number of slots per pole which is a multiple of three, and one would prefer not to have less than six slots per pole. Where the number of slots per pole is a multiple of three, all that is necessary is to lay out one or other of the windings shown in Figs. 111 or 118.

Sometimes, however, we may wish to use a die in which the number of slots per pole is not divisible by three, and sometimes even the number of slots is not divisible by the number of poles, and it is convenient to have a chart at hand which will enable us to say whether a convenient winding can be employed in the particular machine in question, using a given number of slots. It may be said at

the outset that, if we are prepared to introduce slight dissymmetry into the winding, there is hardly any number of slots which may not be used with a given number of

FIG. 119.—Lattice-type coils on the stator of an induction motor.

poles, but leaving out of account for the moment windings which involve some dissymmetry, we may divide the symmetrical cases into five classes.

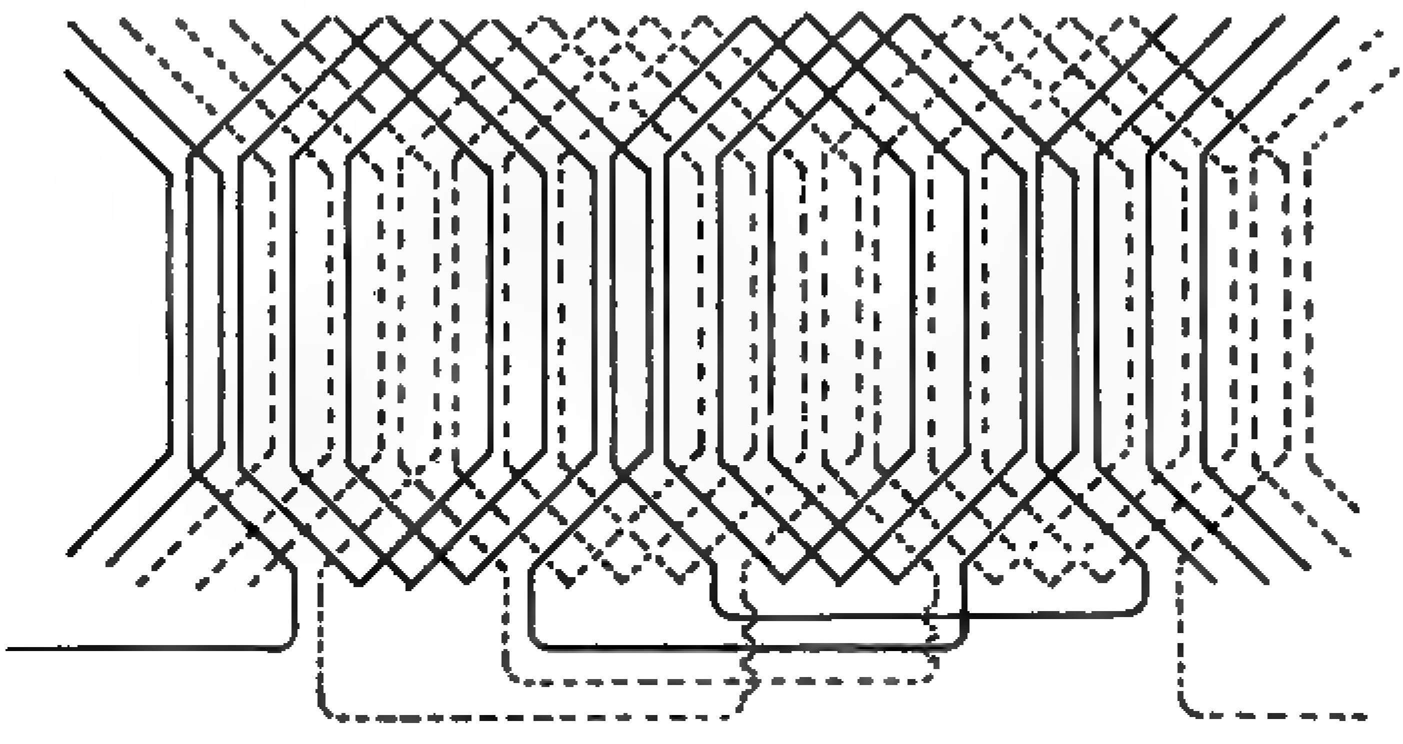


FIG. 120.—Two-phase winding, with lattice end-connectors short chorded: lap wound showing special connectors between groups. Two bars per slot.

Classes of three-phase armature windings.

Class A. *Where the number of slots per pole is divisible by* 3. In these cases one may employ any of the windings illustrated in Figs. 111 to 118, each phase being treated as if it were a single-phase winding. These are the commonest cases in practice.

Class B. *Where the number of slots is one more or one less than a multiple of the pole-pairs, and is at the same time divisible by* 6. Here we may employ a pure wave winding divided into six groups. An example is given below. Into this class also fall the cases where the number of slots is two more or two less than a multiple of the number of pole-pairs. In this class of cases we can employ a duplex wave winding.

Class C. *Where the number of slots is one more or one less than a multiple of the pole-pairs, and is at the same time divisible by* 3. Here we can employ a wave winding, divided into three groups. An example is given below.

Class D. *Where the number of slots is a multiple of the number of pole-pairs, and is divisible by* 6. Here one can employ a wave winding with unsymmetrical end connections, as shown in the example below.

Class E. *Where the number of slots is a multiple of the number of pole-pairs, and is divisible by* 3. Here one can employ the same kind of winding as in D, with certain limitations.

Class F. *Where the number of slots is not such as to fall within any of the above classes,* one may leave certain slots unwound, and make one of the above windings just as if the unwound slots were not there. In cases where the number of unwound slots is a multiple of 3, it is possible to space them so that the three-phase winding when completed is perfectly symmetrical. The main objection to this plan is that, in the hands of an inexperienced winder, some mistake may be made which is difficult to rectify. This class of winding is therefore not usually employed, unless it is imperative to use a certain die in a machine which leaves us no alternative.

We will now give examples of each of these classes.

Class A. Where the number of slots is divisible by the number of poles and then again by 3, so that the number of slots per pole is divisible by 3. Here the conductor diagram is perfectly simple. We may adopt the usual practice of distinguishing the three phases respectively by thick lines, thin lines and dotted lines, as in Fig. 110. It is clear from this diagram that any of the methods given in Figs. 111, 112, 116, 117 or 118 may be employed in making the end connections of the conductors of the different phases. Where concentric connections are employed, as in Fig. 111, the diagram should show the number of tiers or ranges in which the end connections are intended to lie. When there are three tiers they may be arranged in the method illustrated in Figs. 142 and 348 (see page 362). Where a coil winding is employed, each coil containing several turns per slot, the number of slots is generally chosen so as to make a winding of Class A.

CLASS B. The academical single re-entrant wave winding with perfectly uniform end connections requires a number of slots, one more or one less than a multiple of the number of pole-pairs, and if the winding is to be broken into six symmetrical parts, the total number of conductors must be divisible by 6. Very frequently with this type of winding there are two conductors per slot. Here is an example.

A ten-pole machine has 66 slots, with two conductors per slot. The number of pole-pairs $= 5$. Now $5 \times 13 = 65$. Add 1, and we get 66. We can have a throw of 6 on one side and a throw of 7 on the other side, giving a double throw of 13. Starting with the top conductor in slot 1, we go to the bottom conductor of slot 7, then to the top of slot 14, and so on until we arrive at the top of slot 66. If we had not added 1 to our 65 we should (with a constant throw) have arrived at slot 1, and the winding would have been closed too soon. As it is, we pass on according to Winding Table I., and we do not close the winding until we have passed 13 times round the machine. The last step which closes the winding is the step from the bottom of slot 60 to the top of slot 1. This is an example of a retrogressive winding.

Winding Table I. 66 slots, 132 conductors, 10 poles.
Wave winding, Class B. Two bars per slot.

Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.
1	7	14	20	27	33	40	46	53	59
66	6	13	19	26	32	39	45	52	58
65	5	**12**	18	25	31	38	44	51	57
64	4	11	17	24	30	37	43	50	56
63	3	10	16	**23**	29	36	42	49	55
62	2	9	15	22	28	35	41	48	54
61	1	8	14	21	27	**34**	40	47	53
60	66	7	13	20	26	33	39	46	52
59	65	6	12	19	25	32	38	**45**	51
58	64	5	11	18	24	31	37	44	50
57	63	4	10	17	23	30	36	43	49
56	62	3	9	16	22	29	35	42	48
55	61	2	8	15	21	28	34	41	47
54	60								

Now, let us divide this winding into six equal parts, each consisting of 22 conductors. Let one part begin on the top of slot 1 and end on the bottom of slot 5. Let that part be completely disconnected from the rest. Take a second part beginning with the top of slot 12 and ending with the bottom of slot 16, a third with the top of slot 23 and ending with the bottom of slot 27, and so on as indicated on the table, where the first conductor in each section is indicated by the larger type.

Now, it is easy to see that the phase of the E.M.F. generated in the first section of 22 conductors starting in the top of slot 1 is exactly 180° out of phase with the

fourth section of conductors starting from the top of slot 34, because slot 34 is exactly half way round the machine from slot 1, and it occupies exactly the same position with respect to the sixth pole that slot 1 occupies with regard to the first pole. If we therefore reverse the terminals of this fourth section of conductors, we may connect it in series with, or in parallel with, the first section of conductors.

The phase of the second section of conductors, starting from the top of slot 12, is exactly 60 degrees removed from the first section, because when we have reached slot 12 we have climbed through one-third of the 180 degrees between 1 and 34. Similarly, there is a difference of phase of 60 degrees between the E.M.F. generated in the third series beginning with the top of slot 23 and that generated in the second series, because when we have reached slot 23 we have climbed through another third of the 180 degrees. It will be seen that, as there are 6·6 slots per pole, there are 2·2 slots per phase per pole. The top conductors in slots 40 and 39 belong to the first series or phase, while the top conductors in slots 38 and 37 belong to the second, and the top conductors of slots 36 and 35 belong to the third. In some places, however, there are three conductors lying together which belong to the same phase, as, for instance, the top conductors of slots 1, 66 and 65, and the bottom conductors of slots 7, 6 and 5. It is in this way that we get the fractional number of slots per phase per pole.

As the number of conductors, 22, in each section is even, the section ends at the same side of the machine as it begins. Thus all the connections are made on one side. Whenever the number of conductors in a section is odd, the section ends at the side of the machine opposite to that on which it started. It would be very inconvenient to bring connections around the back of the yoke to put the various parts in series or in parallel. When the number of slots is divisible by 6, and there are two conductors in each slot, the number in each series must be even. This is why we draw a distinction between the cases where the number of slots is divisible by 6 and the cases where they are only divisible by 3. In the latter case, as we shall see, a method of winding is still available without bringing connections around the back of the frame.

Windings with one bar per slot. The simplest way of treating wave windings with one bar per slot is as follows: If we take any number which could be used as the number of slots in a two-bar-per-slot wave winding as given above, that number when multiplied by 2 gives a possible number for a one-bar-per-slot winding. We can in this case take two slots, one odd and one even, and regard them as one slot, the odd representing the top of the slot and the even the bottom. Table II. gives the winding table of a one-bar-per-slot winding for the case where there are 10 poles and 132 slots. Here the double throw is 26. The single throw is 13 on each side. Thus we pass from an odd bar to an even, to an odd and so on. By comparing Tables I. and II. we get a clear idea of the relation between a two-bar-per-slot and a one-bar-per-slot winding. Slots 1 and 2 in Table II. just take the place of the top and bottom of slot 1 in Table I.

Now we can go a step further. Having doubled the number of slots, we can if we like employ a duplex winding with two conductors per slot. There

will be two winding diagrams. Each will be exactly the same as Table II., except that the columns will be headed "Top" and "Bottom" alternately. In one table we will begin at the top of slot 1 and go to the bottom of slot 14, then to the top of slot 27 and so on. In the other we will begin at the bottom of slot 1 and go to the top of slot 14 and so on. The total conductors in each of these tables can then be broken up into six sections each of 22 conductors, and the four sections of each phase thus obtained can then be combined either in series or in parallel as desired. In Table VII. we denote this winding by B_2. A sample of a duplex winding is given in Table IV. The terminals of the two windings here will be at opposite ends of the machine. This can be changed by opening the second winding at 12 instead of at 1.

Winding Table II. 132 slots, 132 conductors, 10 poles.

Wave winding, Class B. One bar per slot.

Odd.	Even.	Odd.	Even.	Odd.	Even.	Odd.	Even.	Odd.	Even.
1	14	27	40	53	66	79	92	105	118
131	12	25	38	51	64	77	90	103	116
129	10	**23**	36	49	62	75	88	101	114
127	8	21	34	47	60	73	86	99	112
125	6	19	32	**45**	58	71	84	97	110
123	4	17	30	43	56	69	82	95	108
121	2	15	28	41	54	**67**	80	93	106
119	132	13	26	39	52	65	78	91	104
117	130	11	24	37	50	63	76	**89**	102
115	128	9	22	35	48	61	74	87	100
113	126	7	20	33	46	59	72	85	98
111	124	5	18	31	44	57	70	83	96
109	122	3	16	29	42	55	68	81	94
107	120								

Similarly, if we multiply the 66 by 3 and have 198 slots, we can make a triplex winding. There will be for this three tables. The first of these will begin as follows: Top of 1 to bottom of 19, to the top of 40 and so on. The second table will run: Top of 2 to the bottom of 20, to the top of 41 and so on. The third will begin: Top of 3 to the bottom of 21, to the top of 42 and so on.

These simple independent duplex and triplex windings must not be confused with Arnold re-entrant multiplex windings which are described on page 511.

Class C. This class is distinct from Class B, because the number of slots is not divisible by 6.*

*It may be mentioned here in passing that where the number of slots is divisible by 3 but not by 6, it is possible to employ windings of Class B by putting four conductors per slot arranged in a double barrel winding.

Suppose that we have 10 poles and 69 slots with two conductors per slot. We see that $(5 \times 14) - 1 = 69$. A double throw of 14 will give us a progressive winding, as shown by Table III.

Winding Table III. 69 slots, 138 conductors, 10 poles.

Wave winding, Class C. Two bars per slot.

Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.
1	8	15	22	29	36	43	50	57	64
2	9	16	23	30	37	44	51	58	65
3	10	**17**	*24*	31	38	45	52	59	66
4	11	18	25	32	39	46	53	60	67
5	12	19	26	33	40	**47**	54	61	68
6	13	20	27	34	41	48	55	62	69
7	14	21	28	35	42	49	56	**63**	*1*
8	15	22	29	36	43	50	57	64	2
9	16	23	30	37	44	51	58	65	3
10	17	**24**	31	38	45	52	59	66	4
11	18	25	32	39	46	53	60	67	5
12	19	26	33	**40**	*47*	54	61	68	6
13	20	27	34	41	48	55	62	69	7
14	21	28	35	42	49	56	63		

Now, if we were to divide this wave winding up into 6 equal parts of 23 conductors each, there being an odd number of conductors in each part, it would be necessary when connecting up the various parts to carry connections from the front to the back of the machine. This being undesirable, the following plan may be adopted: Instead of breaking up the winding into parts of 23 conductors each, let the first part have 22 conductors, the second 24, the third 22 and so on. Thus the first series will begin at the top of slot 1 and end at the bottom of slot 10.

The second series will begin at the top of slot 17 and end at the bottom of slot 40 and so on. Thus we will get the six parts. The first conductors of each of these is indicated by the larger type. The italic type indicates the division which would have resulted in an odd number of conductors in each series. It will now be seen that if we connect the first part, (let us call it phase A), in series with the fourth part (which is also phase A), we shall have a series of 46 conductors, which is exactly 120 degrees of phase removed from the series of 46 conductors obtained by connecting the third part (beginning top of slot 47) in series with the sixth part (beginning with slot 40). In connecting the various parts in series regard must be had to the polarity.

It is also possible to have duplex windings belonging to Class C. An example is given in Table IV. of an 8-pole winding in 90 slots.

Winding Table IV. 90 slots, 180 conductors, 8 poles. Duplex wave winding, Class C_2. Two bars per slot.

Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.
1	12	23	34	45	56	67	78
89	10	21	32	43	54	**65**	***76***
87	8	19	30	41	52	63	74
85	6	17	28	39	50	**61**	72
83	4	15	26	37	48	59	70
81	2	13	24	**35**	***46***	57	68
79	90	11	22	33	44	55	66
77	88	9	20	**31**	42	53	64
75	86	7	18	29	40	51	62
73	84	**5**	***16***	27	38	49	60
71	82	3	14	25	36	47	58
69	80						

Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.
1	12	23	34	45	56	67	78
89	10	21	32	43	54	**65**	***76***
87	8	19	30	41	52	63	74
85	6	17	28	39	50	61	72
83	4	15	26	37	48	59	70
81	2	13	24	**35**	***46***	57	68
79	90	11	22	33	44	55	66
77	88	9	20	**31**	42	53	64
75	86	7	18	29	40	51	62
73	84	**5**	***16***	27	38	49	60
71	82	3	14	25	36	47	58
69	80						

CLASS D. Windings of this class are the most generally useful for low voltages, and may be used for voltages up to 3000 on large machines. They have practically superseded the old academic wave windings with symmetrical end connections, except in those cases where the number of slots happens to fit the old winding. The distinguishing feature of Class D is that the number of slots to one pair of poles is a whole number. There may be any whole number of slots per pair of poles: 4, 5, 6, 7, 8, or 9. The only condition is that the total number of slots shall be divisible by 6. Thus, if we have 12 poles and 84 slots, that is 7 slots per pole or 14 per pair of poles, we can make a wave winding falling under Class D. If the winding is to have a full pitch, that is 7 slots, it will begin at the top of slot 1, go to the bottom of slot 8, to the top of slot 15 and so on, as shown in Table V. There are 13 special connectors required with this winding table, each having a throw of 8 slots, from the bottom of 78 to the top of 2, from the bottom of 79 to the top of 3, etc. The method of breaking up the winding into six sections is indicated as before by printing in larger type the first conductor of each section. In this case the first section (beginning with conductor 1) can be put either in

series or in parallel with the fourth (beginning with conductor 8). If there had been an odd number of slots per pair of poles, the winding of this class would still have been possible. For instance, with twelve poles we might have 13 slots per pair of poles, with a throw of 6 on one side of the machine and 7 on the other.

Winding Table V. 84 slots, 168 conductors, 12 poles.
Wave winding, Class D. Two bars per slot.

Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.
1	8	15	22	29	36	43	50	57	64	71	78
2	9	16	23	30	37	44	51	58	65	72	79
3	10	17	24	**31**	38	45	52	59	66	73	80
4	11	18	25	32	39	46	53	60	67	74	81
5	12	19	26	33	40	47	54	**61**	68	75	82
6	13	20	27	34	41	48	55	62	69	76	83
7	14	21	28	35	42	49	56	63	70	77	84
8	15	22	29	36	43	50	57	64	71	78	1
9	16	23	30	37	44	51	58	65	72	79	2
10	17	24	31	**38**	45	52	59	66	73	80	3
11	18	25	32	39	46	53	60	67	74	81	4
12	19	26	33	40	47	54	61	**68**	75	82	5
13	20	27	34	41	48	55	62	69	76	83	6
14	21	28	35	42	49	56	63	70	77	84	7

CLASS E. This class is the same as Class D, except that the total number of slots is only divisible by 3 and not by 6. Here we have recourse to the same plan of dividing up into slightly unequal sections so as to get an even number of conductors into each section. This will be seen at once from Table VI.

Winding Table VI. 75 slots, 150 conductors, 10 poles.
Wave winding, Class E. Two bars per slot.

Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.	Top.	Bottom.
1	8	16	23	31	38	46	53	61	68
2	9	17	24	32	39	47	54	62	69
3	10	18	25	**33**	***40***	48	55	63	70
4	11	19	26	34	41	49	56	64	71
5	12	20	27	35	42	50	57	65	72
6	13	21	28	36	43	51	58	66	73
7	14	22	29	37	44	52	59	67	74
8	15	23	30	**38**	***45***	53	60	68	75
9	16	24	31	39	46	54	61	69	1
10	17	25	32	40	47	55	62	70	2
11	18	26	33	41	48	56	63	71	3
12	19	27	34	42	49	57	64	72	4
13	20	28	35	**43**	***50***	58	65	73	5
14	21	29	36	44	51	59	66	74	6
15	22	30	37	45	52	60	67	75	7

CLASS F. Where the number of slots available does not permit of any of the above symmetrical windings, it is always possible to make an unsymmetrical winding, and where the number of slots is great the dissymmetry can be made so small that the divergence of the angle of phase difference between the phases from the correct 120 degrees will not matter. These unsymmetrical windings can be made by leaving unwound certain slots and making a winding just as if we have the number of slots we require. The unwound slots should be evenly distributed around the armature. Where the number of unwound slots is divisible by 3, it is usually possible to distribute them so that there is no deviation from the angle of 120 degrees between the phases.

The leaving of slots unwound is sometimes deliberate even when a winding could be made using all the slots. In cases where there are very few slots per pole, say only 3, and we are afraid of the wave-form being distorted by the teeth, it is a good plan to depart from the winding which employs a whole number of slots per pole. Suppose that we are designing a 30-pole machine with a very short pole pitch, in which there is room for only three or four slots per pole. Suppose further that to get the E.M.F. we want about 180 or 190 conductors. We would not choose 90 slots even though they are available. It would be better to choose 96 slots and leave 6 slots unwound (see p. 305).

It is convenient to have a table such as Table VII. below, from which one can see at a glance, whether with any given number of slots one can use any of the windings falling under Classes A, B, C, D or E, with a particular number of poles. Suppose that we are designing an 8-pole A.C. generator requiring about 180 conductors, and that we have an armature punching with 90 slots. It is not at first sight evident that we can make a perfectly symmetrical three-phase 8-pole winding with 90 slots and 2 conductors per slot. On referring to the table, we see that we can have a duplex re-entrant winding of the type denoted by C_2. Take 2 from 90 and get $88 = 8 \times 11$. Thus, with a single throw of 11 we will get a retrogressive winding which falls short by 2 conductors each time around the machine. Next to this retrogressive winding there will be another lying in the same slots, and after each has been broken up into its 6 parts, the various parts which are nearly in phase can be connected in series with one another. See page 104 and Table IV. It is interesting to notice in connection with Table VII. that each number of poles has a law of its own as to the numbers of slots that can be used with it. This is seen from the way that the letters giving the types of winding recur in a regular sequence in each column, each column having its own particular sequence. For instance, the sequence in the 10-pole column is A or D, B, C, B_2, E, C_2, C, B, A or D; the only apparent exception to this is that B_2 and C_2 are sometimes interchanged, but even in this there is a law, for we have B_2 when the corresponding number is divisible by 12 and C_2 when it is not. Sometimes we want a winding which shall have some of its terminals on one side of the machine and some on the other, as for instance in the case of an A.C. booster (see page 547). In these cases we choose a number of slots denoted by a C or an E; and by keeping an equal number of conductors in each of the 6 parts, as explained in connection with Table III., we will get what we desire without any dissymmetry. Very often, however,

the number of slots or the number of conductors required will not permit of this, and we will then be compelled to leave out one conductor in order to finish at the side of the machine opposite to that on which we started. The amount of dissymmetry introduced by this is generally of no importance.

Table VII., giving the numbers of slots that can be used with a given number of poles to form a symmetrical 3-phase winding, there being two conductors per slot.

6 Poles.		8 Poles.		10 Poles.		12 Poles.		14 Poles.		16 Poles.		18 Poles.	
No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.
33	*E*	33	*C*	36	B_2	36	*D*	36	*B*	33	*C*	36	*D*
36	*A, D*	36	*D*	45	*E*	42	*D*	42	*A, D*	39	*C*	45	*E*
39	*E*	39	*C*	48	B_2	48	*D*	48	*B*	42	C_2	54	*D*
42	*D*	42	C_2	51	*C*	54	*D*	54	C_2	48	*D, A*	63	*E*
45	*E*	45	*C*	54	*B*	60	*D*	57	*C*	54	C_2	72	*D*
48	*D*	48	*A, D*	60	*A, D*	66	*D*	63	*E*	57	*C*	81	*E*
51	*E*	51	*C*	66	*B*	72	*A, D*	69	*C*	63	*C*	90	*D*
54	*A, D*	54	C_2	69	*C*	78	*D*	72	B_2	66	C_2	99	*E*
57	*E*	57	*C*	72	B_2	84	*D*	78	*B*	72	*D*	108	*A, D*
60	*D*	60	*D*	75	*E*	90	*D*	84	*A, D*	78	C_2	117	*E*
63	*E*	63	*C*	78	C_2	96	*D*	90	*B*	81	*C*	126	*D*
66	*D*	66	C_2	81	*C*	102	*D*	96	B_2	87	*C*	136	*E*
69	*E*	69	*C*	84	*B*	108	*A, D*	99	*C*	90	C_2	144	*D*
72	*A, D*	72	*A, D*	90	*A, D*	114	*D*	105	*E*	96	*A, D*	153	*E*
75	*E*	75	*C*	96	*B*	120	*D*	111	*C*	102	C_2	162	*A, D*
78	*D*	78	C_2	99	*C*	126	*D*	114	C_2	105	*C*	171	*E*
81	*E*	81	*C*	102	C_2	132	*D*	120	*B*	111	*C*	180	*D*
84	*D*	84	*D*	105	*E*	138	*D*	126	*A, D*	114	C_2	189	*E*
87	*E*	87	*C*	108	B_2	144	*A, D*	132	*B*	120	*D*	198	*D*
90	*A, D*	90	C_2	111	*C*	150	*D*	138	C_2	126	C_2	207	*E*
93	*E*	93	*C*	114	*B*	156	*D*	141	*C*	129	*C*	216	*A, D*
96	*D*	96	*A, D*	120	*A, D*	162	*D*	147	*E*	135	*C*	225	*E*
99	*E*	99	*C*	126	*B*	168	*D*	153	*C*	138	C_2	234	*D*
102	*D*	102	C_2	129	*C*	174	*D*	156	B_2	144	*A, D*	243	*E*
105	*E*	105	*C*	132	B_2	180	*A, D*	162	*B*	150	C_2	252	*D*
108	*A, D*	108	*D*	135	*E*	186	*D*	168	*A, D*	153	*C*	261	*E*
111	*E*	111	*C*	138	C_2	192	*D*	174	*B*	159	*C*	270	*A, D*
114	*D*	114	C_2	141	*C*	198	*D*	180	B_2	162	C_2	279	*E*
117	*E*	117	*C*	144	*B*	204	*D*	183	*C*	168	*D*	288	*D*
120	*D*	120	*A, D*	150	*A, D*	210	*D*	189	*E*	174	C_2	297	*E*
123	*E*	123	*C*	156	*B*	216	*A, D*	195	*C*	177	*C*	306	*D*
126	*A, D*	126	C_2	159	*C*	222	*D*	198	*C*	183	*C*	315	*E*
129	*E*	129	*C*	162	C_2	228	*D*	204	*B*	186	C_2	324	*A, D*
132	*D*	132	*D*	165	*E*	234	*D*	210	*A, D*	192	*A, D*		
135	*E*	135	*C*	168	B_2	240	*D*	216	*B*	198	C_2		
138	*D*	138	C_2	171	*C*	246	*D*	222	C_2	201	*C*		
141	*E*	141	*C*	174	*B*	252	*A, D*	225	*C*	207	*C*		
144	*A, D*	144	*A, D*	180	*A, D*	258	*D*	231	*E*	210	C_2		
147	*E*	147	*C*	186	*B*	264	*D*	237	*C*				
150	*D*	150	C_2	189	*C*	270	*D*						
153	*E*												

Table VII. (*continued*), **giving the numbers of slots that can be used with a given number of poles to form a symmetrical 3-phase winding, there being two conductors per slot.**

20 Poles.		22 Poles.		24 Poles.		26 Poles.		28 Poles.		30 Poles.		32 Poles.	
No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.
60	*D*	66	*D*	60	*D*	78	*D*	72	B_2	60	*D*	63	*C*
69	*C*	78	*B*	72	*D*	90	*B*	84	*D*	75	*E*	66	C_2
72	B_2	87	*C*	84	*D*	102	C_2	96	B_2	90	*D*	78	C_2
78	C_2	90	C_2	96	*D*	105	*C*	99	*C*	105	*E*	81	*C*
81	*C*	99	*E*	108	*D*	117	*E*	111	*C*	120	*D*	96	*D*
90	*D*	108	B_2	120	*D*	129	*C*	114	C_2	135	*E*	111	*C*
99	*C*	111	*C*	132	*D*	132	B_2	126	*D*	150	*D*	114	C_2
102	C_2	120	*B*	144	*A, D*	144	*B*	138	C_2	165	*E*	126	C_2
108	B_2	132	*A, D*	156	*D*	156	*A, D*	141	*C*	180	*A, D*	129	*C*
111	*C*	144	*B*	168	*D*	168	*B*	153	*C*	195	*E*	144	*D*
120	*A, D*	153	*C*	180	*D*	180	B_2	156	B_2	210	*D*	159	*C*
129	*C*	156	B_2	192	*D*	183	*C*	168	*A, D*	225	*E*	162	C_2
132	B_2	165	*E*	204	*D*	195	*E*	180	B_2	240	*D*	174	C_2
138	C_2	174	C_2	216	*A, D*	207	*C*	183	*C*	255	*E*	117	*C*
141	*C*	177	*C*	228	*D*	210	C_2	195	*C*	270	*A, D*	192	*A, D*
150	*D*	186	*B*	240	*D*	222	*B*	198	C_2	285	*E*	207	*C*
195	*C*	198	*A, D*	252	*D*	234	*A, D*	210	*D*	300	*D*	210	C_2
162	C_2	210	*B*	264	*D*	246	*B*	222	C_2	315	*E*	222	C_2
168	B_2	219	*C*	276	*D*	258	C_2	225	*C*	330	*D*	225	*C*
171	*C*	222	C_2	288	*A, D*	261	*C*	237	*B*	345	*E*	240	*D*
180	*A, D*	231	*E*	300	*D*	273	*E*	240	B_2	360	*A, D*	255	*C*
189	*C*	240	B_2	312	*D*	285	*C*	252	*A, D*	375	*E*	258	C_2
192	B_2	243	*C*	324	*D*	288	B_2	264	B_2	390	*D*	270	C_2
198	C_2	252	*B*	336	*D*	300	*B*	267	*C*	405	*E*	273	*C*
201	*C*	264	*A, D*	348	*D*	312	*A, D*	279	*C*	420	*D*	288	*A, D*
210	*D*	276	*B*	360	*A, D*	324	*B*	282	C_2	435	*E*	303	*C*
219	*C*	285	*C*	372	*D*	336	B_2	294	*D*	450	*A, D*	306	C_2
222	C_2	288	B_2	384	*D*	339	*C*	306	C_2	465	*E*	318	C_2
228	B_2	297	*E*	396	*D*	351	*E*	309	*C*	480	*D*	321	*C*
231	*C*	306	C_2	408	*D*	363	*C*	321	*C*	495	*E*	336	*D*
240	*A, D*	309	*C*	420	*D*	366	C_2	324	B_2	510	*D*		
249	*C*	318	*B*	432	*A, D*	378	*B*	336	*A, D*				
252	B_2	330	*A, D*	444	*D*	390	*A, D*						
258	C_2	342	*B*	456	*D*								
261	*C*	351	*C*										
270	*D*	354	C_2										
279	*C*	363	*E*										
282	C_2	372	B_2										
288	B_2	375	*C*										
291	*C*	384	*B*										
300	*A, D*	396	*A, D*										

Table VII. (*continued*), **giving the numbers of slots that can be used with a given number of poles to form a symmetrical 3-phase winding, there being two conductors per slot.**

34 Poles.		36 Poles.		40 Poles.		44 Poles.		48 Poles.		56 Poles.		64 Poles.	
No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.	No. of Slots.	Type of Winding.
66	C_2	180	D	180	D	174	C_2	192	D	195	C	192	A, D
84	B	198	D	198	C_2	177	C	216	D	198	C_2	222	C_2
102	D	216	A, D	201	C	198	D	240	D	222	C_2	225	C
120	B	234	D	219	C	219	C	264	D	225	C	255	C
135	C	252	D	222	C_2	222	C_2	288	A, D	252	D	258	C_2
138	C_2	270	D	240	A, D	240	B_2	312	D	279	C	288	D
153	E	288	D	258	B_2	243	C	336	D	282	C_2	318	C_2
171	C	306	D	261	C	264	A, D	360	D	306	C_2	321	C
186	B	324	A, D	279	C	285	C	384	D	309	C	351	C
204	A, D	342	D	282	C_2	288	B_2	408	D	336	A, D	354	C_2
216	B_2	360	D	300	D	306	C_2	432	A, D	363	C	384	A, D
222	B	378	D	318	C_2	309	C	456	D	366	C_2	414	C_2
237	C	396	D	321	C	330	D	480	D	390	C_2	417	C
246	C_2	414	D	339	C	351	C	504	D	393	C	447	C
255	E	432	A, D	342	C_2	354	C_2	528	D	420	D	450	C_2
273	C	450	D	360	A, D	372	B_2	552	D	447	C	480	D
288	B	468	D	378	C_2	375	C	576	A, D	450	C_2	510	C_2
306	A, D	486	D	381	C	396	A, D	600	D	474	C_2	513	C
318	C_2	504	D	399	C	417	C	624	D	477	C	543	C
324	B	522	D	402	C_2	420	B_2	648	D	504	A, D	546	C_2
342	C_2	540	A, D	420	D	438	C_2	672	D				
		558	D	438	C_2	441	C	696	D				
		576	D	441	C	462	B	720	A, D				
				459	C	483	C	744	D				
				462	C_2	486	C_2						
				480	A, D								

The effect of chording the winding. For the values of K_e, given on page 30, we have assumed that we have a full-pitch star winding, that is to say, that the conductors in series with one another are as nearly in the same phase as it is possible to make them in a three-phase star-wound armature evenly distributed over the armature surface. It is convenient sometimes to reduce the pitch of the coils, either for the purpose of changing the E.M.F. or modifying the wave-form, or it may be that for mechanical reasons we may wish to make the coil with a short throw (see pages 113 and 114 for windings employing a short throw). We will consider here the effect on K_e of changing the pitch of the coils.

The most simple method of finding the change which will occur in the resultant E.M.F. when we alter the throw of the coils, or in any way alter the phase of one

part of the winding with respect to the other, is by the vector summation of chords drawn within a circle to represent the various parts of the winding. This method may be described as follows.

We are, of course, considering alternating E.M.F.'s, and we are supposed to be measuring the square root of the mean square values, but what is said of the relation between these values is equally true of the relation between the maximum values, where the wave-form is sinusoidal.

Draw a circle to represent the perimeter of the armature of a two-pole alternator. Take a small arc on this circle, so short that it may be regarded as nearly straight. Let the length of the chord of this small arc represent the sum of the E.M.F.'s generated in a certain number of conductors (it does not matter for this purpose how many), which are placed near together (say, in one slot), and whose E.M.F.'s are nearly in phase with one another. Take 10 of these short arcs, as shown in Fig. 122, so as to obtain a larger arc whose departure from the straight line is noticeable. The ratio between the vector sum of all the little arcs (that is to say, the long chord) and the length of the whole arc is equal to the ratio between the resultant E.M.F. generated in all the conductors lying on the arc and the arithmetical sum of all the E.M.F.'s generated in the same conductors. The ratio of the length of the chord to the length of the arc is the breadth coefficient* of a coil occupying the arc under consideration. The breadth coefficient of a group of coils uniformly distributed over half the circumference of a two-pole armature is the ratio of the diameter of a circle to the half circumference, or $2 \div \pi = 0{\cdot}637$.

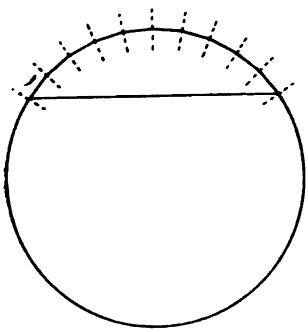


FIG. 122.

Next, we must consider the effect of connecting in series a number of groups lying in different phases.

It is necessary when using the method here described to have a very strict convention as to sign when adding the effects of different phases. The following convention, if carried out strictly, will avoid errors. We are to find the voltage between two terminals of a certain machine, for instance, between the terminals A and B of a three-phase machine. Trace through the diagram of the winding from A to B, and mark on our circle the arc occupied by the various sections of it, adopting the following convention as to sign: If in tracing through from A to B we pass from front to back on the machine in a certain section of the winding, mark the arc on the circle which represents that section with an arrow head† which points clockwise on the circle. For any section of the winding in which we are passing from back to front mark the arc representing that section with an arrow head pointing counter-clockwise.

* The breadth coefficient is $\frac{\sin \sigma}{\sigma}$, where σ is half the angular breadth of the coil (see Fig. 321, p. 305).

This factor $\frac{\sin \sigma}{\sigma}$ is often termed the "winding factor."

† Note that this convention will itself take care of the question of the polarity of the poles. We must, therefore, follow it strictly, never minding the polarity.

After we have arrived at the terminal B, we will have on our circle a number of arcs, each marked with an arrow head. Draw chords to all these arcs, and put an arrow head on the chord to correspond with the arrow head on the arc. The E.M.F. generated in the winding from A to B, will be the vector sum of all the chords, so taken that the arrow heads follow one another consecutively.

In the first example, we will take the straightforward case of an ordinary three-phase generator. The result we know quite well without a vector diagram, but it serves to illustrate the method.

EXAMPLE 19. Take the winding diagram of a two-pole, three-phase, star-wound machine, given in Fig. 116. By way of fixing a datum line, take the centre line of one of the poles as lying on the line EF. Then the centre line of the other pole will be on the line GH, and we have 180 degrees of phase between them. Beginning at A, we trace through this phase to the star point, and in doing so we pass from front to back through conductors occupying a phase-band 60° in width. This phase band we mark off on our circle (Fig. 124 at A_1A_2, and affix the

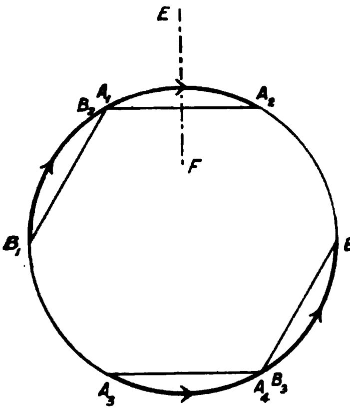


FIG. 124.

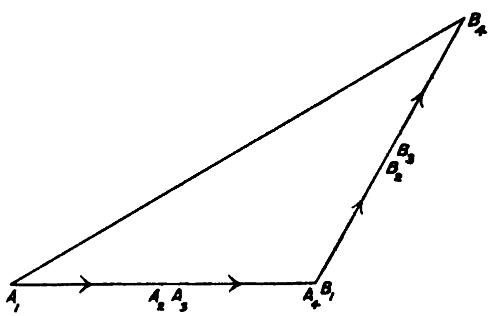


FIG. 125.

clockwise arrow head. At the same time we have passed from back to front through conductors occupying a similar phase band in front of the opposite pole. This we mark off on our circle at A_3A_4, affixing a counter-clockwise arrow head. Passing on again from the star point, we go through phase B, some of the conductors being traversed from front to back. These are marked off on the arc B_1B_2 with a clockwise arrow head. And some of the conductors are traversed from back to front in the position of the arc B_3B_4, and are marked with a counter-clockwise index. Now, the resultant E.M.F. generated in these conductors is proportional to the length of the vector A_1B_4, which is built up of the vectors A_1A_2, A_3A_4, B_1B_2, B_3B_4 (see Fig. 125). In this case the vector A_1B_4 is 0·866 of the arithmetical sum of the small vectors.

In the second example, we will take the case of a two-pole, three-phase winding having a very short throw.

EXAMPLE 20. In Fig. 126 is given the diagram of a two-pole, three-phase winding, lying in 24 slots. The throw of the coils is very short, being just a little over 90°. There are supposed to be two paths in parallel, but for the sake of simplicity the end connectors are only shown on one of the paths. The end-connector diagram for the other path is exactly the same as that shown, except that it is pushed forward 12 slots, or 180°. To find the ratio of E.M.F. generated in this winding to the E.M.F. generated in 4 conductors lying in adjacent slots, we describe our circle as before. In Fig. 127 we have made small circles to note the position of the slots for ease in following the diagram, but this is really unnecessary. We wish to find the E.M.F. at the terminals A and B. Imagine that the centre of one of the poles is, for the instant, opposite

the datum line EF. This gives us the phase position of the vertical line EF (Fig. 127). Trace the winding through from terminal A to terminal B. We pass from front to back

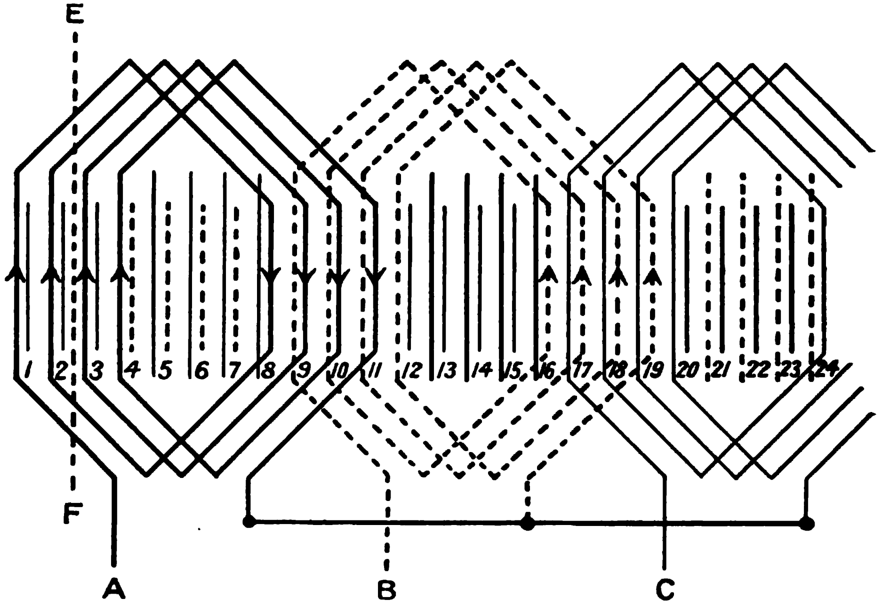


FIG. 126.—A short-chorded, three-phase winding for a two-pole turbo-generator. Throw of coils two-thirds of pole pitch.

through slots 1, 2, 3, 4, and therefore mark a chord (in Fig. 127) of the arc which embraces 1 and 4 with a clockwise arrow head. We pass from back to front through slots 8, 9, 10, 11, and therefore mark the corresponding chord (Fig. 127) with a counter-clockwise arrow head.

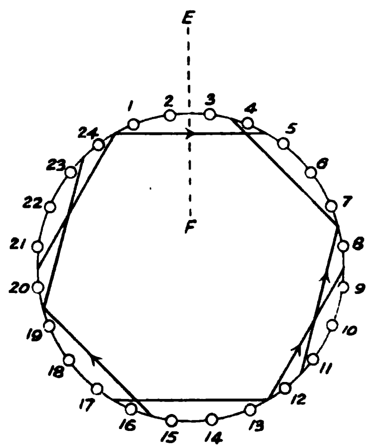


FIG. 127.

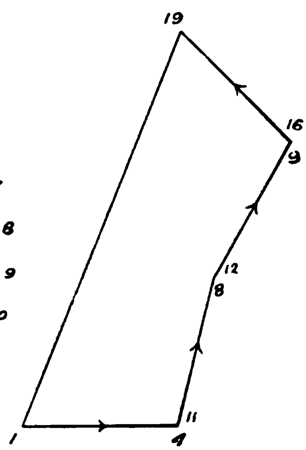


FIG. 128.

Showing method of finding the voltage generated in a short-chorded winding.

This leads us to the star point, from whence we pass into phase B, which leads us from front to back through slots 16, 17, 18, 19, as denoted by the clockwise arrow head in Fig. 127, and from back to front in slots 9, 10, 11, 12, giving us the counter-clockwise arrow head. This brings us

to terminal B. We now make a summation of the vectors, as shown in Fig. 128. The ratio of the length of the vector 1, 19 to the vector 1, 4 is the ratio we require. It will be seen that in this case the E.M.F. generated in the 16 conductors in series in Fig. 126 is only 0·8 of the E.M.F. of the 16 conductors of a similar machine connected as in Fig. 116, as seen from a comparison of the vectors A_1B_4 (Fig. 125) and 1, 19 (Fig. 128). If, therefore, we had a three-phase winding with 8 conductors per phase, which, when connected as in Fig. 116, gave us 550 volts, we could, by chording the winding as shown in Fig. 126, obtain $550 \times 0·08 = 440$ volts.

It is very necessary to follow strictly the convention as to the arrow heads on the diagram, as the vector polygon for some chorded windings will contain vectors which are almost directly opposed to one another, and it is impossible to be sure of the value of the sum unless we have taken strict account of the true phase position of all the components.

FIG. 129.—Barrel end-connectors of three-phase generator armature wound with two bars per slot and connected, as shown in Fig. 118, to generate 440 volts. The bars in this case are bent after being put into the semi-closed slots.

Mechanical arrangement of windings. The mechanical arrangement of the winding will depend upon the type of end connections we have chosen. End

connections of the lattice type permit of a very simple mechanical arrangement. In general, they will form two layers. If these layers lie on a cylindrical or conical surface, we have what is commonly called a "barrel" winding. Such a winding, consisting of solid bars, is illustrated in Fig. 129. In this case there are two bars per slot, one of which is bent to the right and the other to the

Fig. 130.—Barrel winding of stator supported by internal ring of metal bolted to brackets.

Fig. 130a.—Barrel winding showing method of fixing to insulated rings supported on brackets.

left to form the lattice work. Fig. 130 and 130a show methods of fixing a winding of this kind when used on turbo-generators. Fig. 135 shows a somewhat similar method of clamping.

A barrel winding is frequently built up with wire-wound coils. Such a winding on the stator of an induction motor is shown in Fig. 119.

Where such a winding forms part of the revolving element of the machine, it is usual to support it mechanically by means of a wire band or end bell. Fig. 131 shows an ordinary C.C. armature with barrel winding. Fig. 133 shows a similar winding on the rotating field-magnet for a turbo-generator. The advantages of

FIG. 131.—Barrel winding on C.C. armature ready for banding.

FIG. 132.—"Short"-type winding on a C.C. motor armature.

this type of winding are that it can be easily formed into shape, easily insulated in a satisfactory manner, and where the radial thickness of the winding is not too great, the cooling is very good.

Where the throw of the coils is great, as in two-pole machines, this type of winding projects rather a long way from the armature iron, and therefore takes

Fig. 133.—Barrel winding on revolving field-magnet of turbo-generator, with six bars per slot. There are six unwound slots per pole.

more copper than some of the other types. In cases where there is very little end room, as in railway motors, this winding is modified to form the "short" type shown in Fig. 132 (see page 163). Where the throw of the coils is short, as in machines having six poles, or a greater number of poles, the "barrel" winding is very economical in material.

On stationary armatures the "barrel" winding is sometimes formed from coils arranged so that there is only one limb of a coil per slot. Such a winding is illustrated in Fig. 134.

The "barrel" winding will be found useful in cases where it is desired to make the throw of the coils much shorter than the pitch and interleave the different phases. In this case all coils must be completely insulated to stand full voltage to earth and between phases.

Another arrangement of lattice end connectors employed in stator windings is to bend up the conductors until they lie at 30° or 45° to the axis of rotation. This winding can be secured by means of suitable brackets. Fig. 135 shows the armature winding of a 10,000 K.V.A., three-phase, 2400-volt, 60-cycle, four-pole generator built by the Westinghouse Electric & Manufacturing Co. of America.

FIG. 134.—Barrel winding formed with wire-wound coils, one coil in two slots, showing method of bracing coils to the cast-metal supporting ring (British Thomson-Houston Company).

The coils have a throw of only two-thirds of the pole pitch, so as to make the end connectors shorter (see Fig. 126). Thus each phase of the winding occupies a coil-breadth of 120 electrical degrees, and this has the effect of eliminating the action of any third harmonic even if the phases are connected in mesh (see page 307). As coils belonging to different phases lie in the same slots,* each complete coil must be insulated all over to withstand full pressure to ground. Armatures

* With a chorded winding, the eddy-currents (see p. 144) in an armature conductor may be smaller than with a full-pitch winding. To calculate the eddy-current loss, we can use the curves given in Figs. 167 and 167a, but we take fractional values for m. A. B. Field has given the following values for m for the case where there are two conductors per slot, the current in the two conductors being out of phase. For the inner conductor $m=1$. For the outer conductor we have $m=2$ for zero phase difference; 1·82 for 60° difference; 1·62 for 90° difference; and 1·0 for 180° difference. For a three-phase armature short-chorded by 60°, the eddy-current loss generally comes out some 75 per cent. of the value of a non-chorded winding.

of this type have withstood repeated short circuits. The spaces between coils permit of excellent ventilation. If we bend up the conductors still more, until they lie in a plane at right angles to the axis of rotation, we have what is commonly called an involute winding. Such a winding is illustrated in Figs. 136 and 137.

The advantage of this arrangement is, that it enables a winding having a long throw to be made without extending the length of the armature too much

FIG. 135.—Stator of 10,000 K.V.A., three-phase, 2400-volt, 60-cycle, four-pole generator, showing type of lattice winding at an angle of 45° to the axis.

in the direction of the shaft. It can be conveniently clamped against the flat face of the end plate, but requires considerable radial depth where the throw is great. Such an arrangement would result from any of the lattice diagrams given in Figs. 103, 105, 107 or 110. Where, however, the end-connector diagram is like Fig. 104, the involute does not show a continuous lattice work. Fig. 117 shows an involute winding with short-throw end connectors. This winding can also be

supported mechanically by means of clamps fixed by bolts passing through the openings in the winding.

FIG. 136.—Involute stator winding.

For **hand-wound** induction motors with **semi-closed slots**, the type of winding illustrated in Fig. 138 is commonly used. This is analogous to an involute winding, but the coils are merely formed of cotton-covered wire wound promiscuously and bent into a skew shape so as to clear one another. Coils of this type can be put turn by turn into semi-closed slots which have been previously insulated, the mouth of the slot being subsequently closed by wooden or paper wedges. This is sometimes called a "*mush*" winding. In Fig. 139 is shown a "mush" winding on the rotor of an induction motor. In Fig. 140 the first inserted coils are seen forced back so as to allow the last coils to be put in.

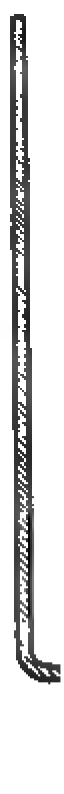


FIG. 137.—Arrangement of clamp on involute winding.

Concentric coils. Where each coil consists of a number of turns and it is required to insulate the phases very well from one another, some makers prefer to use concentric coils so as to preserve larger insulating spaces between coils belonging to different phases than is generally possible with the lattice type of end connector. If the slots are closed or semi-closed, the concentric coils are sometimes wound by hand through insulating tubes, the end connections being subsequently insulated. Hand winding cannot be recommended where the voltage is high. Wire-wound coils are much more satisfactory when formed and impregnated before being placed in the slots. Open slots are therefore desirable where the number of conductors per slot is great.

Where the number of conductors per slot is few—say one to six—a very satisfactory method is to make the part of the coil which lies in the slot of straight conductors insulated, impregnated and wrapped. After these have been placed in the slot (which in this case can be made closed or semi-closed), the **end connectors**, which have likewise been previously insulated, can be **jointed** to the slot conductors, each joint being thoroughly insulated as it is made. The advantage of this method of winding is that it enables the conductors which lie in the slot to preserve their perfect straightness throughout the whole insulation treatment, and a more satisfactory coil, free from air spaces and bulgy insulation, can be made than where a large coil is insulated as a whole. Moreover, in case of a breakdown it is possible to remove any one coil of this type without disturbing the remainder. The joints between the straight conductors and the end connectors will not cause any difficulty if they are not too numerous, and if plenty of space is allowed for jointing and insulation. A concentric winding of this type, made in two tiers, is illustrated in Fig. 141 (compare Fig. 112). The method of making the joints is seen in Fig. 142, which shows a winding in three tiers (compare Fig 111). For *single-phase windings*, such as illustrated in Fig. 102, a concentric coil is very suitable, and offers no special difficulties. For large machines, however, where the span of the coil is great, they are sometimes bent back against the end plate, because in this position they can be more securely fixed by means of clamps.

FIG. 138.—"Mush" winding on the stator of induction motor, put wire by wire through the mouths of semi-closed slots and subsequently insulated.

Where the number of poles is not a multiple of four, it is possible to employ a two-tier winding by

FIG. 139.—"Mush" winding on the rotor of induction motor, put wire by wire through the mouths of semi-closed slots and subsequently insulated.

using on the one pair of poles two skew coils, as illustrated in Fig. 115. The three-tier winding is the most convenient to employ with concentric coils on two-pole and six-pole machines.

The forces which come into play when the winding of an electric generator is short circuited or when an unexcited armature is thrown suddenly on to a high-voltage main. In discussing the mechanical arrangement of armature windings it is necessary to say something about the enormous forces which come into play when a winding is short circuited. Any generator or motor is liable to this accident, and it should be so constructed that it will not be seriously injured if the accident should occur. The designer must be able to say what windings require special bracing, and what windings are sufficiently strong without bracing.

In general there are two kinds of accidents to consider. First, the case where a generator is running fully excited, and is short circuited at or near its terminals, and secondly, the case where a machine standing idle is inadvertently switched on to the supply mains. In the first case we have, before the short circuit, a high E.M.F. within the winding which takes an appreciable interval of time to fall to a low value. The strength of the current depends upon the characteristics of the machine itself. In the second case, there is no E.M.F. on the winding before the switch is closed, and the length of time that the E.M.F. is exerted depends upon the characteristics of the generators supplying the mains and the operation of any cut-out devices which may be in circuit.

FIG. 140.—"Mush" winding on the rotor of an induction motor, showing the method of getting in the last coils.

We will take first the case of a short-circuited alternate-current generator.

If the armature of an ordinary alternator is short circuited while the machine is at rest, and the machine is then run up to speed and fully excited, the current in the armature will not in general rise to more than $2\frac{1}{2}$ or 3 times its full-load value. This is because the current lags almost 90 degrees behind the phase position of pole centre and it demagnetizes the poles (see page 282). This current would not be sufficient to bring into play any serious forces on the winding. But if an alternator, while running at full speed and fully excited, is suddenly short circuited at the terminals of the armature, the current may rise to more than 20 times its full-load value. The first rush of current is propelled by the full E.M.F. of the generator, because there is not sufficient time during the first cycle after the short circuit for the field magnet to be demagnetized to any considerable extent. The rate at which the current rises is determined by the

self-induction of the armature winding. In considering the armature self-induction which is effective immediately after a short circuit, we must take only that part which is due to the flux leaking across the slots, along the air-gap and around the end windings.

There cannot be an instantaneous weakening of the field-magnet, because, as the current in the armature rises, there is an eddy current in the pole face, or

FIG. 141.—Two-tier concentric winding of 6000 K.V.A. three-phase generator (British Westinghouse Company).

in the field winding itself, which maintains the flux from the pole almost at its full value. It is only as this eddy current dies down that the armature demagnetizes the field.

The eddy current in the pole or in the field winding (or it may be in both), while exerting a magnetomotive force to keep the main flux of the pole in existence notwithstanding the demagnetizing effect of the armature current, sets up around itself a leakage flux in the field-magnet, and this leakage flux opposes the rise of the eddy current and makes the rate of rise smaller than it otherwise would be.

In order that we may have a rough picture of what is happening, let us take a particular case, the case where a short-circuit occurs in phase A of a turbo-

generator whose field-magnet is made of solid steel of the type shown in Fig. 350. We may, in order to fix our ideas, consider actual values that one finds in practice. Let Fig. 150 represent a three-phase 5500-K.W. generator with four solid salient poles revolving at 1000 R.P.M. The total flux per pole amounts to 78,000 kilolines; there are 27 conductors per phase per pole, and the instantaneous value of the E.M.F. generated in phase A at the moment when the pole is in the position shown in Fig. 150 is 9000 volts. The resistance of phase A is 0·056 ohm. The magnetic flux l_1, which leaks across the slots and around the end windings of

FIG. 142.—Concentric coil winding in three tiers made with rectangular strap and bolted to end plate (British Westinghouse Company).

the machine when 1 ampere passes in phase A amounts to 6·2 kilolines per pole. For full-load current—288 amperes (=405 amps. maximum)—the leakage therefore amounts to 2500 kilolines, or 3·2 % of the total flux per pole.

In order to simplify matters, the figures given here for the flux per pole are reduced to allow for the breadth coefficient, and the leakage flux is dealt with in the same way. Thus, if N = effective flux per pole,

$$\text{volts per phase} = 2\pi n \frac{Z}{2} N \times 10^{-8}, \quad \text{where } \frac{Z}{2} = \text{the turns per phase.}$$

Rate of rise of the current. Now, let phase A be short circuited at the moment when the pole is in the position shown in Fig. 150. There is an E.M.F. of 9000 volts tending to drive current through phase A. As the current rises it

will not only set up leakage λ_1, but it will create a magnetomotive force in all such paths as *mm*, threading through the path of the iron pole. As soon as any flux begins to grow in the path *mm*, it immediately produces a current in the pole face of an amount almost equal to the total current in the phase band *A* and opposite to it in direction. In the case in Fig. 150, with counter-clockwise rotation of the *N* pole, the current in the phase band *A* will flow towards the observer from the paper; the eddy current in the pole will flow from the observer towards the paper. The return path for this eddy current will be along the sides of the pole and back along the face of an *S* pole. This current opposes the creation of flux along the path *mm*, so that the flux cannot grow at a greater rate than is just sufficient to generate the eddy current against the opposition of the resistance and self-induction of its path. The self-induction of

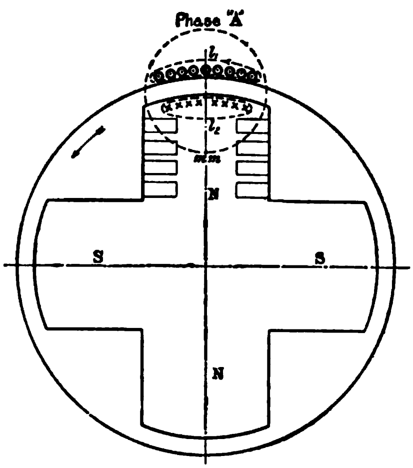


FIG. 150.

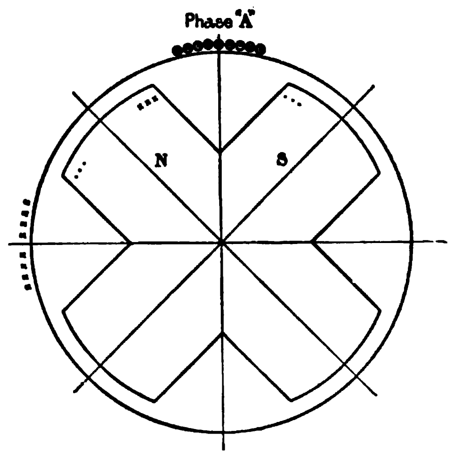


FIG. 151.

Showing eddy currents in the pole-face and the paths of the leakage flux λ_1 and λ_2.

the eddy-current path is caused by magnetic leakage, which may be represented by the symbol λ_2. As the flux along *mm* grows, it will set up a back E.M.F. in phase *A*, so that the effect will be just as if the resistance and self-induction of the eddy-current path in the pole were transferred to phase *A*. The value of the resistance and coefficient of self-induction of the eddy-current path in the pole would be difficult to calculate in any particular case, but in the case considered below, the coefficient of self-induction (the more important term), when multiplied by the ratio of transformation, appears from the result of experiment to have a value equal to about 2·4 times the coefficient of self-induction of that part of the armature winding which lies opposite the pole. We therefore have the leakage $\lambda_1 + \lambda_2$ as the main controlling factors in determining the rate at which the current *I* in phase *A* begins to rise; taking $\lambda_1 + \lambda_2$ in the above case to be 21·2 kilolines per ampere, the rate at which the current would begin to rise is 800,000 amperes per second.

If the short-circuit occurs in *A* at the instant when the poles are in the position shown in Fig. 150, the current I_A cannot rise higher than such a value as will make

the leakage flux equal to the working flux per pole, that is, $I_{sh}(\lambda_1+\lambda_2)=N$. If, however, the short-circuit occurs when the poles are in the position shown in Fig. 151, the total change of flux threading through phase A, as a North pole is replaced by a South pole, is $2N$; so that the limiting value of the short-circuit current is such that $I_{sh}(\lambda_1+\lambda_2)=2N$.

Fig. 152 shows generally the way that the short-circuit current would rise, if we leave out of account the effects of resistance and capacity. The height to which it will rise depends upon the instant at which the short circuit occurs. If the short circuit occurs when the voltage is at its maximum, the current begins

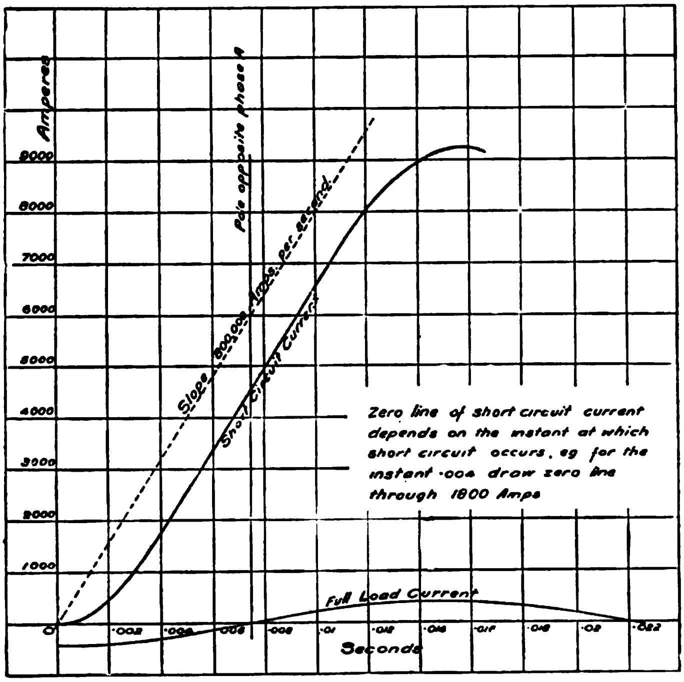


FIG. 152.—The magnitude and phase of the instantaneous short-circuit current as compared with full-load current lagging 90°.

to rise at its maximum rate, but it cannot rise for longer than one quarter of a period. We therefore must in that case draw the zero line for the current curve through the ordinate 4600 in Fig. 152, and get a current curve symmetrically placed with regard to the zero line, the maximum being not more than 4600 amperes. If, however, the short circuit occurs at the instant when the voltage in phase A is at zero and becoming positive, then the current will have twice as long a time in which to rise, and will rise to nearly twice the value, or nearly 9200 amperes. It does not rise to quite twice the value, by reason of the fact that the pole is being gradually demagnetized, and the resistance of the armature begins to have an appreciable effect at the higher values of the current.

As the pole moves from the position shown in Fig. 150, the eddy current moves along the pole face as though it were a reversed reflection of the current in

the phase band A mirrored in the face of the pole, and shortly after the corner of the pole has left phase A, there is an eddy current in the pole face down one side and up the other, which tends to keep up the flux in the pole against the strong demagnetizing current in phase A, which is now in the best demagnetizing position (see Fig. 151).

If instead of a solid pole we have a laminated pole, the resistance of the path for the eddy current is very much higher, but it is found that the exciting current in the winding is increased at the instant of short circuit, and takes the place of the eddy current in keeping the value of the pole flux nearly constant for the instant after short circuit. By the time that the pole S comes under phase A, the eddy currents are beginning to diminish, so that pole S is not as strong as pole N was. The current falls under the influence of pole S, and rises again under the influence of the next N pole, so falling and rising, and describing a waving line which keeps its mean value above the zero line for several alternations. It may be said that at the instant of short circuit a direct current is generated in the winding which is maintained by the self-induction of the winding and slowly killed by the resistance. Superimposed on this direct current is an alternating current generated by the passing of the poles alternating N and S, and at the same time gradually growing weaker as the eddy current in the pole face or in the exciting windings grows weaker. After a few seconds the poles become normally excited, and the current sinks to $2\frac{1}{2}$ or 3 times its full-load value.

There is a difficulty in dealing with this subject analytically, because the eddy paths in the poles are not of simple form, and their resistance and self-induction vary as the eddy current takes up different positions in the poles. We know, however, that there is produced at the instant of short circuit a large magnetizing eddy current. We also know that the magnetizing eddy tends to live by reason of the self-induction of its path, but is slowly killed by the resistance of the path, so that we may assume that it changes with time t' approximately as the expression $I_e\epsilon^{-\frac{r_2}{l_2}t'}$, where I_e is its maximum value and r_2 and l_2 are the values of the resistance and coefficient of self-induction of the path, though these are not necessarily capable of being expressed by constants. Now the E.M.F. in the armature at any instant may be regarded as consisting of two parts, one part e_s, the E.M.F. which is sufficient to drive the final short-circuit current through the armature against its resistance and self-induction, and the evanescent part e_e, the E.M.F. generated by the flux from the pole which is produced by the eddy current in the pole.

$$e_s = E_s(\sin 2\pi nt),$$

$$e_e = \epsilon^{-\frac{r_2}{l_2}(t-t_1)}E_e(\sin 2\pi nt).$$

Here we have written $t' = (t - t_1)$ in order to have only one time variable. t_1 is the time at which the switch is closed.

At the instant of short circuit $(t - t_1) = 0$ and $e_s + e_e = e$, the full E.M.F. of the machine. As the eddy current in the pole dies out, e_e disappears and $e = e_s$, and the machine then gives its normal short-circuit current. The expression

for the current at any instant after a short circuit therefore takes the form

$$I=\frac{E_s+\epsilon^{-\frac{r_2}{l_2}(t-t_1)}E_e}{\sqrt{r_1^2+4\pi^2n^2l_1^2}}\{\sin(2\pi nt-a)\}-\frac{E_s+E_e}{\sqrt{r_1^2+4\pi^2n^2l_1^2}}\epsilon^{-\frac{r_1}{l_1}(t-t_1)}(\sin 2\pi nt_1-a).$$

The last term of this expression corresponds to the evanescent term which always appears in the expression for the current after switching on. As t_1 is a constant, the last term represents a current*, which is always on the same side of zero, possibly very great at the instant of switching on, and slowly dying as it is killed by the resistance of the armature winding. The angle a of course equals

$$\tan^{-1}\frac{2\pi nl_1}{r_1}.$$

In the above expression r_1 and l_1 are the apparent resistance and coefficient of self-induction of the armature winding. We say "apparent," because if there are any circuits (such as eddy-current paths in surrounding iron) which carry currents induced by the armature current, the resistance and self-induction of

* It may make the matter clearer to some readers who are not very familiar with switching phenomena if we consider the current which flows in a single-phase circuit whose resistance is R and inductance L when we suddenly switch on a voltage following the law: $E_1=E_0\sin pt$.

The instantaneous value of the current

$$i=I_0\sin(pt-\phi)-\epsilon^{-\frac{R}{L}(t-t_1)}I_0\sin(pt_1-\phi),$$

where $\quad I_0=\dfrac{E_0}{\sqrt{R^2+p^2L^2}},\quad \phi=\tan^{-1}\dfrac{pL}{R},$ and $t_1=$ the time of closing the switch.

The wave-form i in Fig. 153 shows the values of i when t_1 is just a little less than half a period, and where L is great compared with R.

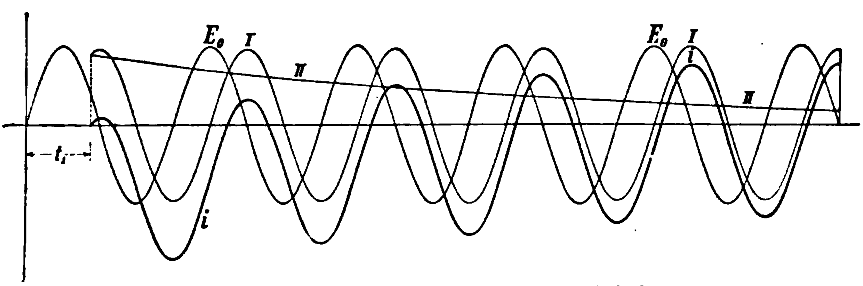


FIG. 153.—Wave-form of current suddenly switched on.

A simple way of arriving at such curves as these is as follows: We know that, finally, after a number of cycles the current will settle down to the value

$$i=I_0\sin(pt-\phi).$$

Plot this wave-form as shown at I. Now we know that the current at the instant of switching must be zero. In order that it may be zero, there is superimposed upon I a unidirectional current $-I_0\sin(pt_1-\phi)\epsilon^{-\frac{R}{L}(t-t_1)}$, which is shown plotted and marked II. This is equal to $I_0\sin(pt-\phi)$ at the instant t_1, and being subtracted from it makes the current zero. It has the effect of displacing the zero line of I by an amount that is always decreasing because of the factor $\epsilon^{-\frac{R}{L}(t-t_1)}$.

In the formula given at the top of this page, we are concerned with two evanescent factors: one of these controls the unidirectional current which is superimposed as in Fig. 153 because of the sudden switching on, and the other controls the rate of decrease of the maximum voltage generated because of the dying eddy current in the field-magnet.

these circuits must (after multiplying by the proper ratio of transformation) be added to the true resistance and self-induction of the armature, just as with a transformer the resistance and self-induction of the secondary of a transformer are transferred to the primary.*

In order to ascertain the instantaneous value of the current in the armature at the time of short circuit some records were taken on a Duddell oscillograph fitted with a cinematograph film. Fig. 154 shows one of these records taken on a machine of the type shown in Fig. 150. The curve V shows the voltage before the short circuit, which, in this case, is 3900 virtual, the maximum point being 5700 volts. At the instant of short circuit the voltage at the terminals of the machine falls to zero, and the current curve springs into being; the highest current recorded was 3100 amps. The curve is sufficient to show the general nature of the current on short circuit. It will be seen, as indicated in Fig. 151, that it rises to such a high value during the first half of a cycle that the pole which passes during the next half cycle is just sufficient to bring it to zero. It then rises and falls in waves of gradually diminishing amplitude, until after a lapse of several

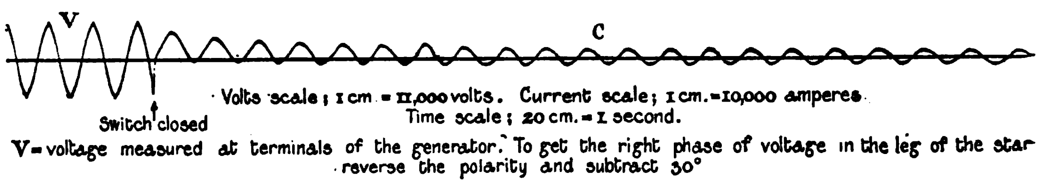


FIG. 154.—Oscillograph record of armature current when an alternator is short circuited. Volts before short circuit = 3900 virtual. Maximum current 3100 amperes.

seconds it assumes the value it would have had if the short circuit had been made before the field was excited. The value of the current at the first peak is, in this case, 7·6 times as high as the maximum value of the current after it has settled down.

It will thus be seen that the amount of current which flows when a winding is short circuited is determined by voltage on the winding and the coefficient of self-induction of the winding, and in calculating the self-induction we must take into account only those magnetic paths around the winding through which magnetic lines can pass without setting up opposing eddy currents. On page 422 we have given a simple method of calculating the leakage flux across a slot, and on page 425 we give rules for roughly estimating the leakage flux around end windings. If the sum of these fluxes for one pole in stator and rotor, when one ampere is passing in

* The following are the values of the different quantities as calculated from the oscillograph curves taken on short circuiting the 5500-K.W. generator above referred to:

$E_s = 975$ volts maximum in one phase.

$E_e = 2740$ " " "

$r_1 = 0{\cdot}105$ ohm.

$l_1 = 0{\cdot}011$ henry.

$\frac{r_2}{l_2} = 2{\cdot}06$ during first half-second, but changes slowly to 1·15.

$n = 33\frac{1}{3}$.

$t_1 = 0{\cdot}002$; t is taken so that the volts of one phase of the generator $= 3715 \sin 2\pi nt$.

the armature, is taken, and denoted by l_1 and l_2, then the highest possible value of the short-circuit current is approximately $I_{sh} = \frac{2N}{l_1 + l_2}$, when N is the flux from one pole. We multiply by 2 on account of the doubling effect which may occur at some instants of switching.

FIG. 155a.—Stator and rotor current of 10,000 K.V.A. turbo-generator. Single-phase short circuit occurring when the voltage is nearly zero.

The experiments are more fully described by the author in a paper,* reference to which is given below. The reader is referred to this paper for other curves and deductions therefrom.

Some very interesting oscillograph curves taken during the short circuiting of a 10,000 K.V.A., 2400-volt, 60-cycle, 4-pole generator, are given by A. B. Field.†

FIG. 155b.—Single-phase short circuit occurring when the voltage is near its maximum.

Two of these are reproduced here. Fig. 155a shows the short circuit at an instant when the voltage was near zero, and Fig. 155b shows the short circuit at an instant when the voltage was near its maximum. In the first case the middle value of the current curve is very much displaced from the current zero line, and in the second

* "Short Circuiting of Large Electric Generators and the Resulting Forces on Armature Windings," *Jour. Inst. Elec. Engrs.*, vol. 45, page 295.

† "Operating Characteristics of Large Turbo-Generators," *Amer. Inst. Elec. Engrs.*, vol. 31, page 963.

case it is very little displaced, as we might expect from the foregoing theory. In both cases the maximum change of current in a half-cycle, that is, the amount measured from the top of the positive peak to the bottom of the negative peak, is the same. It is, in fact, the current which could produce a leakage flux nearly equal to twice the pole flux.

In Fig. 154 a curve has been drawn through the crests of the positive waves, and similarly one through the crests of the negative waves, and these have been extended back to the axis drawn for the instant of short circuit. The two curves intercept this axis, marking off a length PQ corresponding to 37,000 amperes. For a given machine the length of this intercept is almost independent of the particular instant at which the short circuit occurs. It is therefore useful as a characteristic of the machine. It may be taken as roughly proportional to the flux per pole, and inversely proportional to the leakage flux per ampere in the armature. It also depends upon the number of phases short circuited. The curves given in Fig. 154 (*b*) and (*c*) relate to a single-phase short circuit at half normal voltage. The lower curves in these figures show how the exciting current varied. The following interesting data are given of the machine in question. With the rotor removed and an external source of 60-cycle current applied to the stator terminals, the impedance was found to be such as to give approximately 8·4 times normal current with full three-phase voltage applied, and 7·3 times normal current when the rated voltage was applied across two only of the three terminals. Similar tests made on this machine with the rotor in place indicated an impedance which was not strictly independent of the magnitude of the current, but which apparently would give about 12 times normal current with three-phase full voltage, and about $10\frac{1}{2}$ times normal current single phase. The rotor was of the solid steel type like that depicted in Fig. 350, and the stator is shown in Fig. 135. The air-gap was $\frac{7}{8}$ inch at each side, and the stator slots 0·86 inch wide. The power absorbed on the impedance test with the rotor in place amounted to 340 K.W., and less than one-sixth of this when the rotor was removed. This increase in power was, of course, due to the heavy eddy currents in the face of the stationary rotor.

Changes of proportions which improve the regulation of the generator do not of necessity cause an increase in the momentary short-circuit current. In particular, it should be noted that a high ratio between the ampere-turns on the field magnet and the ampere-turns on the armature (a feature which gives good current regulation) does not in itself increase the short-circuit current. The short circuit is kept down by decreasing the flux per pole or by increasing the armature leakage per ampere (see page 388).

Switching in when out of step. If two similar alternators running at full speed and fully excited are thrown in circuit with one another when directly out of step, the currrent which flows through the armature is the same as if each machine had been short circuited at its terminals. The E.M.F. taken in the whole circuit of the two machines is doubled and the resistance and self-induction are also doubled. If the machines are thrown into circuit only partly out of step, the current which circulates is not so great, being equal to the short-circuit current multiplied by the sine of half the angle of phase displacement between the two machines. Where two machines are feeding a busbar and a third similar

machine is thrown on to the busbar directly out of step, the current flowing in the latter machine will be one-third greater than if it were short circuited at its terminals. This is because the total E.M.F. in circuit is doubled, while the resistance and self-induction of the circuit are only increased in the ratio of 3 : 2. Where three machines are feeding a busbar and a fourth is thrown on to the busbar directly out of step, the current is 50 per cent. greater than if the machine were short circuited at its terminals; and so on, as the resistance and self-induction of the machines in circuit with the busbar become less and less. The maximum effect will be obtained where a machine is switched on to a busbar fed by a very large number of generators. In this case the current might rise to almost double the value it would attain on a dead short circuit; *i.e.* the forces which would come into play would be nearly four times as great.

Next consider the case of a generator or motor *switched suddenly on to the line*. The currents through the armature winding in these cases may be even greater than where the E.M.F. is generated in the machine itself. We may, for instance, have a very large power station, the voltage of which is very little affected by the drawing of a large current. Where the self-induction of the windings of the generator or motor is fairly high, the current will be limited by this circumstance. If, for instance, an engine-type generator has laminated poles and the field circuit is open, the current which will flow through the armature or switching on will not be as great as if the poles are solid or if the field circuit is closed. If an induction motor has a squirrel-cage rotor, the current flowing on switching the stationary machine on to the busbars is much greater than if the rotor is of the wound type and is open circuited. The leakage flux of induction motors is usually such a large percentage of the total working flux that, even with a squirrel-cage rotor, the current on switching on a dead machine is not sufficient to cause serious trouble. The question which the designer must ask himself is: "What is the value of the self-induction of the winding which would be operative in cutting down a current on switching on, and what are the chances of the machine being switched on under circumstances likely to injure the winding?" Then a rough calculation of the forces upon the winding will tell him whether it is necessary to specially brace the winding or not, and what kind of bracing ought to be employed.

Forces on the windings. The study of the instantaneous currents which flow when an alternator is short circuited is important on account of the great forces which they bring into play upon the armature windings. In fact, there has been considerable difficulty in the past in devising adequate means of supporting the coils. Even with slow-speed generators, it was known that the sudden rush of current which occurred on short circuit, or when the generator was thrown on the busbars badly out of phase, would injure the winding unless it were made very strong and suitably supported. But it was not until after many serious accidents that the designer realized how many times greater were the forces he had to deal with in the case of turbo-generators. The reasons for the difference in this respect between slow-speed machines and turbo-generators are as follows: In the first place, the high-speed machine has comparatively few poles, and therefore the

ampere-turns per pole are very much greater. For instance, a 3000-K.W. 25-cycle engine-type generator, running at 94 revs. per minute, may have about 2000 ampere-turns per phase per pole, while a 3000-K.W. 25-cycle turbo-generator, running at 1500 revs. per minute, may have as many as 8000 ampere-turns per phase per pole. The force is proportional to the square of the current, so that four times as many ampere-turns will give very many times as much force. A case is worked out in the author's paper already referred to.

Secondly, the span of the coils in the engine-type machine is very much shorter. In the machines compared above, the spans of the coils might be 18 in. in the one case and 90 in. in the other.

Then, again, the magnetic flux leaking across the slots and around the end windings bears a much smaller ratio to the total flux per pole in the case of many turbo-generators than it does in the case of engine-type machines. This consideration, as we have seen above, is one that determines the value of the current on short circuit.

The troubles that were experienced in the early turbo-generators were, no doubt, partly due to the disinclination on the part of the designer to bring the high-tension winding very near to metal clamps. Experience had shown that it was desirable to preserve long-creepage distances and to keep coils of different phases as far away from each other as possible; any clamping that had to be done was done in accordance with old-established rules of insulation. The result was that in many cases the clamping was insufficient.

It will be seen that when we are dealing with forces of many hundreds of pounds per foot run, especially when many coils are grouped together, very strong clamps are necessary to keep the windings in position. The old plan of tying coils together with torpedo twine and securing them with wooden blocks is wholly insufficient. One cannot hope to make a satisfactory construction without using strong metal clamps. This necessitates insulating the whole of the winding outside the slots with an insulation which is not only strong enough to withstand the whole testing pressure, but is of such a good mechanical nature that it will not be crushed under the pressure of the clamps.

We may consider here the various methods of clamping the windings of turbo-generators.

Where the winding is of the barrel type the clamping may be carried out in the manner shown in Figs. 130 and 135. Two main objects must be kept in view in designing this clamping. First, the individual coils must be stayed so that they cannot move relatively to one another, and secondly, the winding as a whole must be prevented from being attracted to the nearest part of the frame. One advantage of the barrel winding is that the field produced by some of the conductors is to a certain extent neutralized by the field produced by conductors lying near, so that the magnetic field over a great part of the coils is not as great as with other types of winding. The magnetic field, however, at the point c in Fig. 130, is as great as with any other type, and, at this point, the coil is difficult to support. With this winding the conductors belonging to different phases lie next to one another, and it is very necessary to insulate the coil throughout its whole length with insulation strong enough to resist full pressure to earth. It is usual to

impregnate the coils as a whole and to place them in open slots, the coil being secured by wedges in the top of the slot. Various methods of securing the parts of the coils lying outside the slots are given in the paper quoted above.

MATERIAL FOR CONDUCTORS.

Copper is almost universally employed for conductors in the armatures of electrical machines, because of all commercial metals it occupies the least space* for a given current-carrying capacity. In addition to this advantage, it has many excellent mechanical qualities. It is easily drawn to wire and strap; its ductility enables it to be bent without much fear of breaking; it can, moreover, be readily welded and soldered, and the action of the air upon it does not create any deleterious oxide. It is thus an ideal material of which to make the conductors in dynamo-electric machines. Its price, however, is high, and it has been suggested from time to time that other metals—notably aluminium—might be employed.

The use of aluminium. Some firms are now using aluminium wire instead of copper wire for field coils. Aluminium is always covered with a thin *film of oxide*, and this film can, by certain chemical processes, be made substantial enough to act as an insulator between successive turns of a wire coil when the voltage per turn is very low. Thus it is possible to use wire without any cotton covering, the oxide being relied on as insulator between turns, a thin sheet of paper being used between the layers of wire to prevent short circuiting. The *conductivities* of aluminium and copper are in the ratio of 1 to 1·66 at 15° C., so that if an aluminium wire of the same resistance must be used, it must have a cross-section of 1·66 times that of the copper wire which it replaces. If both wires are round or both square, the aluminium wire will have a diameter 29 % greater. In the case of small wires, where the cotton covering makes the diameter so much greater than the bare copper as to allow room for the sheet of insulation, an aluminium wire takes up less room than the copper cotton-covered wire.

In many cases it is not necessary to keep the size of the aluminium coil strictly within the space limits of the copper coil. On machines where there is room it will often pay to use aluminium,† even though the coil is much more bulky.

It is difficult to give any reliable figures of the comparative cost of copper and aluminium coils, because the labour is in some cases a large item and differs widely in different factories. For coils of thick wire such as tramway coils, where the cost of material is great as compared with the cost of labour, the saving on the use of aluminium amounts to 25 or 30 %. In the case of small wire coils of, say, No. 28 or 30 S.W.G., the material will cost 80 or 100 % more

* "Electrical Conductivity of Copper," Wolff and Dellinger, *Amer. I.E.E. Proc.*, 29, p. 1981, 1910; "High-conductivity Cast Copper," E. Weintraub, *Amer. Elec. Chem. Soc. Trans.*, 18, p. 207, 1910; "Conductivity of Copper," Hirobe and Matsumoto, *Elektrot. Zeit.*, 33, p. 1245, 1912.

† The patent rights for the United Kingdom and the Colonies of the Spezialfabrik für Aluminium Spulen und Leitungen, G. m. b. H. Berlin, are held by the Manchester Armature Repair Co.

for single cotton-covered copper wire than for aluminium wire, occupying the same room and having the same resistance. But the aluminium wire must be wound layer for layer instead of "mush," as is often done with cotton-covered wire, and if this is done by hand the labour comes out rather high. For cylindrical coils which can be wound by machinery the aluminium coil is certainly cheaper and in many respects better.

In addition to the saving in cost there are several other advantages claimed for aluminium coils. The absence of the cotton covering makes the heat conductivity very much greater than for coils of cotton-covered wire. It must, however, be remembered that enamelled copper wire* is now very widely used. The heat conductivity of this, allowing for thin sheets of paper between layers, is almost as high as the heat conductivity of aluminium with similar sheets of paper.

The good **heat conductivity** of the aluminium coil leads to a rather lower mean temperature and to a corresponding lower resistance; so that, one may sometimes employ a cross-section considerably less than 1·66 times that of the replaced copper wire without exceeding the prescribed temperature rise. Thus, in the series field coils of traction motors, it has been found by experience that in using aluminium wire it is only necessary to increase the section to 1·4 times the section of copper wire. When square aluminium wire is employed for field coils, the heat conductivity from wire to wire is extremely good. The insulating oxide **withstands vibration** very well, and **cannot be destroyed by heat,** so that field coils of this kind can be made which give very satisfactory service. For coils of this kind the paper between layers is replaced by asbestos. As the **weight** of an aluminium coil is only one half that of a copper coil, the handling during the process of manufacture is very much easier, and some saving is made in freight. In the case of traction motors† the saving in weight is of special importance. Some particulars of standard railway motor field coils wound in aluminium are tabulated below:

Type of Motor.	Maker.	Weight of field coil in lbs.		Weight saving per car, lbs.
		Copper.	Aluminium.	
GE 800	B. T. H. Co.	65	29·5	142
GE 52	,,	47	21·2	206
GE 58	,,	60	27·0	264
DK 25A	Dick Kerr & Co.	40	18·0	176
W 3A	Westinghouse	64	29·0	280
GE 66A	(i) B. T. H. Co.	136	68·0	390
	(ii) ,,	57	28·5	
B 17/30	Siemens & Halske	117	53·0	266

There are not many cases in which, at present prices, it would pay to use aluminium for **armature conductors**, but if the price of aluminium falls very much below the price of copper the loss of space may, to a certain extent, be counterbalanced.

* "Black Enamelled Wire," *Elect. Rev.*, N.Y., v. 51, p. 611, 1907.

† "Aluminium Windings for Field Magnets of Traction Motors," A. Mariage, *Lumière Electr.*, 14, p. 104, 1911.

There are some machines in which the room taken up by the active conductors is not a vital factor in determining the size of the frame. For instance, in the armatures of A.C. turbo-generators, which are external to the field-magnet, there is usually plenty of room for the conductors, particularly if the voltage is low (see page 274). A case of this kind is considered below.

Suppose we had to build a 3750-K.V.A. 25-cycle generator, running at 1500 R.P.M., to deliver 3200 amps. at 650 volts, three phase. We should find that the size of the machine would be determined by the size of the rotating field-magnet, and whether we use a small slot or a somewhat larger slot hardly affects the cost of the frame. Thus there would be no difficulty in finding room for an aluminium conductor. Moreover, the voltage being low, the cost of the insulation would be small as compared with the cost of the metal.

As it would not be convenient to provide more than two paths in parallel on a two-pole armature, each conductor might be designed to carry 1600 amps., and it

FIG. 156.—Stranded copper conductor to carry 1600 amperes.

FIG. 157.—Stranded aluminium conductor to carry the same current as in Fig. 156 with the same temperature rise.

would be desirable to use a stranded conductor, which would occupy the space shown in Fig. 156. In this size of conductor, we have allowed 1 watt per sq. in. cooling surface. Now, if a stranded aluminium conductor of the size shown in Fig. 157 were used, the cooling surface would be increased 28%, and thus would permit of the use of a conductor having only 44% greater cross-section than the copper conductor for the same allowance of cooling surface per watt. It would be seen that even at the present prices of the metals (copper at £80 a ton and aluminium at £90 a ton) there is a theoretical advantage in using the aluminium in this case, and if the prices were reversed, there is little doubt that the difficulties at present in the way of using aluminium would be overcome in such cases as this.

Again, on the rotors of induction motors there is often plenty of room to use aluminium instead of copper, and the ease of casting the cage in position warrants the change. It must be remembered that the mechanical qualities of aluminium are not so good as those of copper, and there is not with it the same ease in making thoroughly satisfactory electrical joints. It must be remembered, too, that the amount of insulation taken to envelop an aluminium conductor is greater than when copper is employed.

High-resistance metals. It is sometimes an advantage to increase the resistance of conductors without reducing their size, as, for instance, in the rotors of crane motors of the squirrel-cage type, where resistance is necessary in order to give the motor the right characteristics, and where considerable substance is required in the conductors in order that they may not heat up at too great a rate. In such cases brass conductors have been employed; in others, copper conductors are placed in the slots, and these are connected to rings of high-resistance alloy.

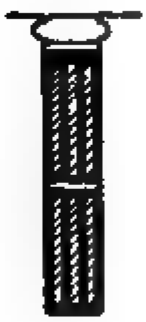


Fig. 158.

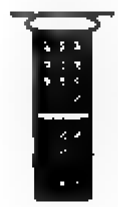


Fig. 159.

Shape of conductors. Round copper wire is most generally useful where the size is small, and where it is necessary to follow tortuous paths necessitating bending in various directions. The round insulated wire presents no sharp corners by which to injure its insulation, whereas square wire is only satisfactory where such control can be kept over the position of the corners as will avoid danger to the insulation. Where this can be done, square or rectangular wires offer advantages in giving a larger space factor, better heating

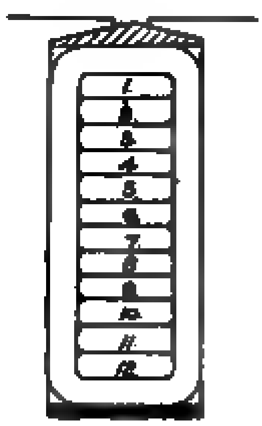


Fig. 160.

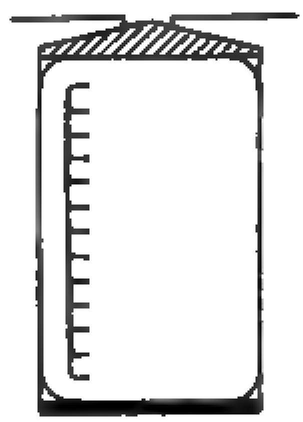


Fig. 161.

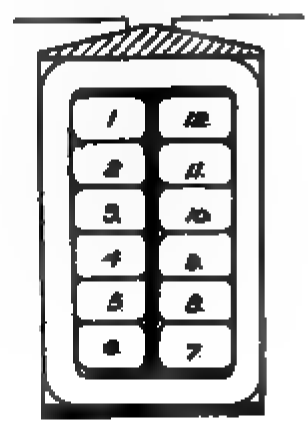


Fig. 162.

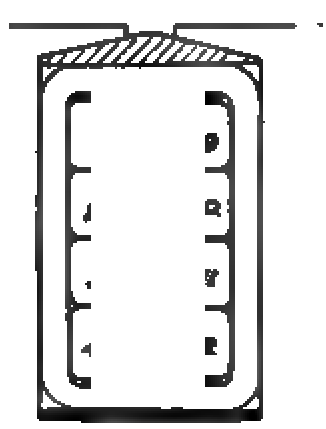


Fig. 163.

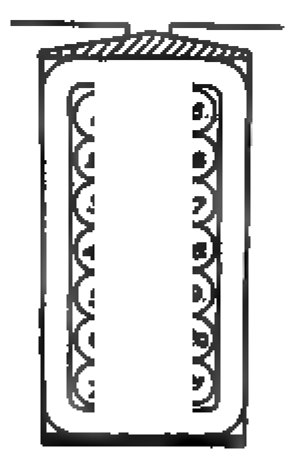


Fig. 164.

Methods of arranging conductors in slots.

conductivity, and it is often possible with rectangular wires to make a better arrangement of the armature conductors so as to keep down the voltage between adjacent turns.

Arrangement of conductors in armature slots. In continuous-current armatures and other low-voltage armatures in which a "barrel" winding is employed, a

common arrangement of conductors is that shown in Fig. 158. In this case the conductors near the bottom of the slot form part of one coil, and those at the top of the slot part of another coil, the general scheme of winding being as shown in Fig. 505. Where many small conductors are required, the arrangement shown in Fig. 159 is commonly employed, the coils being wound with small rectangular copper straps. For smaller wires still, it is better to use round wires. In many cases complete coils are built up of sections wound on formers, as described on page 151. In other cases the slot is filled with wires which have no definite arrangement, as shown in Fig. 44. This type of winding is commonly known as a "mush" winding. The copper space factors for these different types of windings depend, of course, upon the thickness of insulation used.

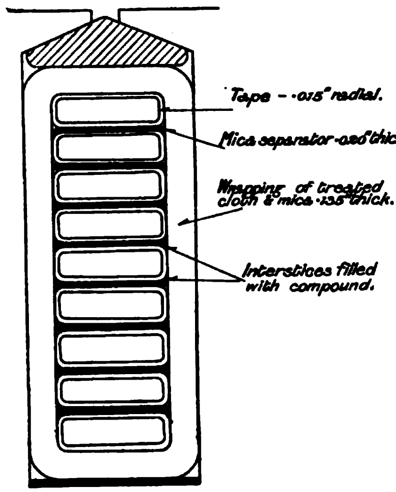


FIG. 165.—Section through slot of an 11,000-volt 3-phase star-connected generator. Showing best method of grouping and insulating for high stresses.

In alternate-current generators and in induction motors operated at a high voltage, the arrangement of conductors is sometimes carried out in the manner indicated in Figs. 160 to 165. The arrangement shown in Figs. 160, 161 and 165 is to be preferred, because in this case the voltage between adjacent conductors is never more than the voltage of one turn. The heat conductivity with this arrangement is also good. In Fig. 161 the two conductors in each pair, 1, 1; 2, 2; etc. are in parallel. Two conductors are used in preference to one wide one in order to get greater ease when bending on edge.

The shape of conductors in armatures is effected by considerations as to the eddy currents which will be produced in them. This matter is considered fully on page 144.

Arrangement of conductors on field-magnets. Some field-magnets are wound with distributed windings, resembling ordinary armature windings. In these cases the conductors can be arranged as illustrated in Figs. 133, 215, and 220. Where the field flux of the magnet remains fairly constant, one is not afraid of eddy currents, and, therefore, the radial depth of the conductors may be made very much greater than would be permissible in an armature carrying an alternating current. Fig. 371 shows the arrangement of conductors in the slots of a turbo field-magnet, in which very deep conductors are used.

In the exciting coils of field-magnets rectangular straps will be used where the current is very large (100 amps. or more). For smaller currents rectangular or square wires will in general be preferred to round wire down to size about ·072 in diameter. Below this size it is more convenient to use round wire. In any case, round wire is often used up to large sizes on account of the ease with which it is wound, and as the layers of large round wire bed very well into one another, the space factor is fairly good.

Space factor in wire-wound coils. The space factor* in wire-wound coils depends on the thickness of the insulation and the closeness with which the successive layers are bedded into one another.

In Fig. 166 the space factors of various sizes of round wire with various types of insulation are given.

Curve A gives the factor for round wire double cotton-covered where the wires are arranged in square order, and curve B gives the factor where they are arranged in close order. The curve C shows the possible space factor in

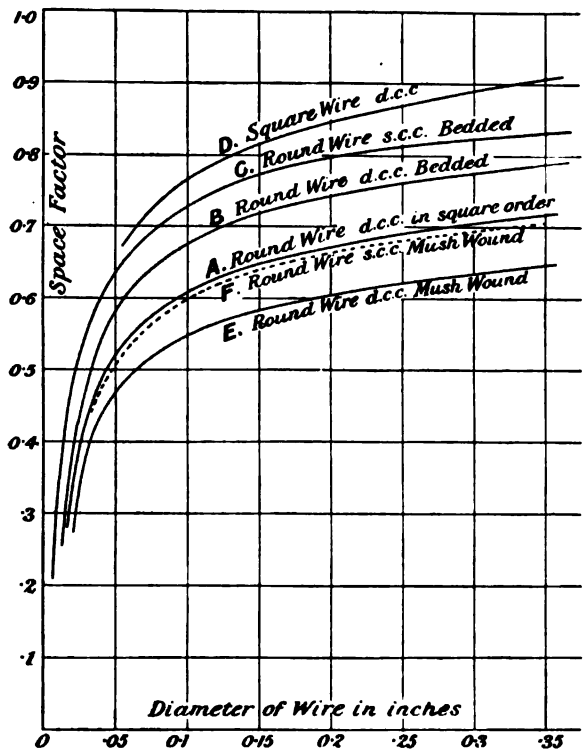


FIG. 166.—Space factors of different sizes of round and square cotton-covered wire.

coils wound with single cotton-covered wire. Where the coil is not wound turn for turn, but in mush fashion, the copper factor is much lower, as shown in curve E for double cotton-covered wire and curve F for single cotton-covered wire. These curves have been plotted from values obtained from ordinary cotton coverings. Manufacturers of covered wire supply specially fine cotton coverings which give rather better space factors than those given in Fig. 166. Curve D shows the space factor, which can be obtained by carefully winding double cotton-covered square wire.

* See "Factors Governing Space Utilization of Electromagnet Windings," C. R. Underhill, *Elec. World*, 53, p. 155, 1909.

Where square wires are employed, a better space factor can be obtained, as will be seen from Fig. 163.

Flat straps have come much into use instead of square wires, because they can in general be adjusted more nearly to fit given sizes of slots than square wires, and, moreover, the wider flat faces compel the straps to lie more closely than is found to be the case with square wires which have received a small accidental twist.

The arrangement of a number of flat copper straps as illustrated in Fig. 163 has decided advantages over an arrangement of the same number of square wires. In Fig. 163 no two adjacent conductors have a greater voltage between them than the voltage of one turn.

SIZE OF CONDUCTORS.

The current density which can be used in any conductor will depend upon the cooling conditions and upon the permissible temperature rise. The matters which effect the cooling conditions are—(1) The thickness of the insulation; (2) the number of conductors assembled together; (3) the temperature of the surrounding iron or air; and (4) the possibility of heat being conducted away along the conductor. These matters are considered at greater length in the chapter on "Heat Paths." The designer knows approximately the current density which can be employed in the type of winding he is employing; and having taken a conductor of suitable size, he finds the total cooling surface and the number of watts lost in the conductor, and adjusts the size until the cooling conditions are sufficiently good (see page 222). He knows that, in low-voltage bar-wound armatures, the current density for 40° C. rise may range between 2500 and 3500 amps. per square inch. In high-voltage armatures the current density will range from 1500 to 2500, while in shunt coils the current density may be 1000, 500, or even fewer, amps. per square inch.

The following methods of calculating the size of conductor will usually be sufficient, though in cases where it is important to cut down the copper to the smallest possible limit more refined methods of calculation, such as described in Chapter X., will be employed.

Shunt coils. The size of wire for the shunt coils of a field-magnet is determined by the length of the mean turn of the coil and the voltage to be applied to the coil. The resistance of one turn of the coil must be such that, if the voltage on the coil were applied to that one turn, the current which would flow is equal to the total ampere-turns required to be carried by the coil. Any multiplication of the turns multiplies the resistance and divides the amperes by the same factor, and as the number of turns is increased the watts required to give the required ampere-turns become less. The size of wire then is fixed by the ampere-turns required, the voltage in the coil and the length of mean turn, while the number of turns determines the watt loss.

The following formulae give the cross-section of the wire required for a given number of ampere-turns, A.T., a given mean length of turn, l_t, and a given voltage per coil, V.

$$\frac{\text{A.T.} \times 7{\cdot}5 \times 10^{-7} \times 1{\cdot}2 \times l_t''}{V} = \text{cross-section of wire in sq. ins.}$$

Or, in centimetre measure,

$$\frac{\text{A.T.} \times 1{\cdot}7 \times 10^{-6} \times 1{\cdot}2 \times l_t}{V} = \text{cross-section of wire in sq. cms.}$$

The factor 1·2 is introduced on the assumption that the temperature rise of the coil will be 50° C. above the atmosphere, which is taken at 15° C.

The formulae are also correct if A.T. represents the total ampere-turns on the poles, and V is the voltage across all shunt coils connected in series.

In the practical calculation of shunt coils the main consideration is the provision of sufficient cooling surface to keep the coil cool. In Chapter X. some data are given relating to the cooling of wire-wound coils. For the present purpose it is sufficient to assume that we know from experience the number of square inches of cooling surface to be allowed for every watt lost. For instance, the usual figure with moderate speed continuous-current generators, having only natural ventilation, is 2·5 sq. ins. per watt, where the permissible temperature rise is 40° C.

Now we know, from previous measurements on the frame with which we are dealing, the approximate cooling surface on each shunt coil. Divide this surface by 2·5 (if that is the number of sq. ins. per watt to be allowed). We now arrive at the total watt loss permissible in that coil. Knowing the voltage at the terminals of the coil (due allowance being made for volts absorbed on the rheostat), we divide the watts by the voltage and obtain the current. The number of ampere-turns on the coil divided by the current gives us the number of turns per coil, and multiplying the number of turns by the length of mean turn we get the total length of wire. The voltage divided by the current gives us the required resistance of the coil, so the resistance divided by the number of thousands of feet gives us the resistance per thousand feet. Having obtained the size of wire, we must see whether the number of turns of that wire can be put into the available winding space. If they cannot, and if the cooling surface which we have taken cannot be increased, or the cooling conditions improved, then it is not possible to obtain the number of ampere-turns on the pole in question without having a higher temperature rise (see p. 504).

EXAMPLE 21. A certain 250-volt, continuous-current, 6-pole generator, running at 800 R.P.M., requires 6200 ampere-turns per pole at full load. The length of mean turn is 44 inches, or 3·66 feet, and the total cooling surface available is 770 sq. ins. per coil. What is the size of wire and number of turns required on the assumption that we are allowing 2·5 sq. ins. per watt, and a margin of 15% in the rheostat at full load?

$$\frac{770}{2{\cdot}5} = 308 \text{ watts per coil.}$$

The voltage on the whole shunt winding (deducting 15% for the rheostat) is 212. Dividing this by 6, we get 35·3 volts per coil.

$$\frac{308}{35{\cdot}3} = 8{\cdot}7 \text{ amps.,}$$

$$\frac{6200}{8{\cdot}7} = 712 \text{ turns per coil,}$$

$$712 \times 3{\cdot}66' = 2610 \text{ feet per coil,}$$

$$\frac{35{\cdot}3}{8{\cdot}7} = 4{\cdot}06 \text{ ohms,}$$

$$\frac{4{\cdot}06}{2{\cdot}61} = 1{\cdot}55 \text{ ohms per 1000 feet.}$$

We now look in the wire table for a wire having a resistance at 70° C., of about 1·55 ohms per 1000 feet. Now, No. 13 S.W.G. wire 0·92" diameter has a resistance of 1·46 ohms at 70° C. We might choose this wire and allow for a greater margin on the rheostat, or we may wind 140 turns of No. 14 wire and the remainder with No. 13 wire, if the cost of making the joint is less than the difference in the cost of the wire.

We now try if the above number of turns will go in the winding space, which in this case may be $8\frac{1}{2}'' \times 1''$. This would allow 80 turns per layer and 9 layers = 720 turns in all. We now calculate the length of mean turn more exactly, and the cooling surface, and see that the allowance per sq. in. is sufficient.

Series coils. The size of copper strap to be used in a series coil is sometimes settled from the circumstance that the whole winding must not have more than a certain resistance. In other cases it is sufficient that the heat generated by the passage of the current shall not cause an excessive temperature rise. In the latter cases a rough estimate must be made of the cooling surface available, and the size of strap fixed, so that the watts per sq. in. are not too great. The rules for calculating the watts per sq. in. are given in Chapter X. (see pp. 230 and 489).

Calculation of the length of mean turn. The only accurate method of finding the mean length of turn of an armature or field coil of an electrical machine is from a lay-out on a drawing board, but for the purpose of making quotations on machines for which no drawings have been made, it is well to have quick simple rules for estimating lengths.

For barrel-wound armatures having approximately a full-pitch winding, a simple rule is to add the length of the iron to 1·4 times the throw of the coil in inches, and to this add 3 inches. Multiply this by the total number of conductors.

Calculation of resistance and weight. An easy rule for getting the resistance of 1000 feet of any size of conductor (at 15° C.) is to divide 0·0082 by the cross-section in square inches.

0·0082 ohm is, of course, the resistance of a conductor 1 sq. in. in section and 1000 feet long.

To get the weight in lbs. per thousand feet of a conductor, remember that a conductor of 1 sq. in. section and 1000 feet long weighs 3800 lbs., so merely multiply 3800 by the cross-section in sq. inches.

If we are working in metric units, divide 0·17 by the cross-section in square centimetres, and we get the resistance of 1000 metres of the conductor. To get the weight in kilograms of 1000 metres, multiply the cross-section in sq. cms. by 890.

EXAMPLE 22. An 8-pole C.C. armature has 768 conductors each of $0·06'' \times 0·5''$ copper strap. The length of the armature is $10\frac{1}{2}''$, and the throw of the coils 13". Find the weight of copper and the resistance of the 8-pole lap-wound armature.

$$10\tfrac{1}{2} + (13 \times 1·4) + 3 = 31·7,$$

$$31·7 \times 768 \times \tfrac{1}{12} = 2020 \text{ feet},$$

$$0·06 \times 0·5 = 0·03 \text{ sq. in.} \quad \frac{0·0082}{0·03} = 0·273 \text{ ohm per 1000 feet},$$

$$2·020 \times 0·273 = 0·55 \text{ ohm all in series},$$

$$\frac{0·55}{64} = 0·0086 \text{ ohm with 8 paths in parallel},$$

$$0·03 \times 3800 \times 2·02 = 230 \text{ lbs. weight of copper.}$$

In some cases, as, for instance, in induction motors and generators of high voltage, the straight part of the coil will project several inches from the slot, and in these cases we must add more than the 3 inches. In the case of some continuous-current machines, where there is a considerable length of conductor from the end of the armature coil to the commutator, something more must be added. For concentric coils of the type shown in Fig. 112 a simple rule is to add the length of iron to the pitch of the poles, and to this add A inches, where A is 12″ for voltages up to 1000, 16″ for voltages up 3000, 20″ for voltages up to 6000 and 22″ for voltages up to 11,000. Where the type of winding differs from Fig. 114, it is easy to concoct a simple rule of this kind for the mean length of turn which will give the weight of copper and the resistance sufficiently near for the purpose of estimating.

The mean length of turn on a shunt coil depends upon the depth of coil and the amount of space allowed between the pole and the inside of the coil. In practice, the designer knows from the frame he is using, and the methods of mounting the coil, how much to add to the two sides of the pole in order to get the half mean length. Where the coil is a fairly tight fit on a rectangular pole, it is sufficient to add twice the depth of the winding to the sum of the two sides of the rectangular pole to get the half mean length of turn.

EXAMPLE 23. A rectangular pole measures $8\frac{1}{4}'' \times 10\frac{1}{2}''$, the depth of winding is $1\frac{1}{2}''$. There are 800 turns of No. 14 S.W.G. wire (0·005 sq. in.) and 8 poles on the machine. Find the resistance and weight of the 8 coils.

$$8{\cdot}25 + 10{\cdot}5 + 3 = 21{\cdot}75 \text{ for the half mean turn,}$$

$$21{\cdot}75 \times 2 \times 800 \times 8 \times \tfrac{1}{12} = 23{,}000 \text{ feet,}$$

$$\frac{0{\cdot}0082}{0{\cdot}005} = 1{\cdot}64 \text{ ohms per 1000 ft.,}$$

$$23 \times 1{\cdot}64 = 37{\cdot}8 \text{ ohms,}$$

$$0{\cdot}005 \times 3800 \times 23 = 440 \text{ lbs. of wire.}$$

Eddy currents in armature conductors. A very important matter to be considered when fixing upon the size and shape of armature conductors is the eddy current, which is induced in the body of the conductor by the magnetic field set up either by the current in the conductor itself or by the currents in the neighbouring conductors. We will confine our attention to conductors placed in armature slots, because the surface-wound armature is seldom employed. When the conductor is in a slot, the eddy current may be produced either by a magnetic field which travels down the slot parallel to the length of the tooth or by a field which crosses the slot from side to side. Except in those cases where the iron of the tooth is very highly saturated, the magnetic field passing down the slot is so weak that it does not, in practice, cause any trouble from eddy currents. In cases where the teeth are highly saturated, the width of any individual conductor measured across the slot should be kept as small as possible. The eddy-current loss in watts per cubic centimetre in it can be calculated by the following formula:

$$W_e = \frac{\pi^2}{6} \times \frac{1}{\rho} \times t_c^2 \times n^2 \times B_{\max}^2 \times 10^{-16},$$

where t_c is the thickness of the copper strap measured at right angles to B, n is the frequency and B_{max} the maximum flux-density threading through the conductor. Where the product of nt_c is great the induced eddy current interferes with the impressed B, so that to get a correct result it is necessary to allow for the change in B due to the eddy, but for values of nt_c less than 25 we may take B_{max} at the impressed value. We may take ρ for warm copper at 2×10^{-6}.

It will be seen from the following example that this loss is usually of very little importance:

EXAMPLE 24. $B_{max}=400$ (corresponding to about $B=20000$ in the teeth),

$$n=50,$$
$$t_c=\cdot 25,$$
$$W_e=8\cdot 2\times 0\cdot 0625\times 2500\times 160000\times 10^{-11}.$$
$$=0\cdot 00205 \text{ watt per cu. cm.}$$

Where, however, the teeth are highly saturated (say to 25000), and the conductors are thick, the eddy-current losses at 50 cycles become appreciable. This matter has been investigated experimentally by Dr. Ottenstein, and for further information the reader is referred to his paper,* which deals also with losses due to flux fringing from the sides of the teeth.

The eddy current which is produced by the magnetic field which passes across the slot, and which is usually produced by the current carried by the conductors in the same slot, may be very great indeed if the conductors are not properly designed. This matter has been fully dealt with in a paper† by A. B. Field, read before the American Institution of Electrical Engineers, in which the theory is fully worked out, and some very useful curves given by means of which the eddy current in any case can be arrived at in a very simple manner. Figs. 167 and 167a are reproduced by permission from Mr. Field's paper, and examples showing how they are employed are worked out below.

The amount of the eddy-current loss is a function of the radial depth of the conductor, and also of the current which passes in the slot between the point at which the eddy current is being considered and the bottom of the slot. When there are a number of conductors one above the other, the conductor nearest the mouth of the slot, being in the strongest field, has the greatest eddy-current loss. In Fig. 167 the curve marked m_1 refers to a conductor nearest the bottom of the slot; m_2 refers to the conductor next to it, and so on, the higher numbers of m being nearer the mouth of the slot. If there is only one conductor, the curve m_1 refers to it. The eddy-current loss is also

* "Das Nutenfeld in Zahnarmaturen und die Wirbelstromverluste in massive Armatur-Kupferleitern," *Sammlung electrotechnischer Vorträge*, Stuttgart, 1903.

† *Proc. Amer. Inst. Elec. Engrs.*, vol. 24, p. 761, 1905. See also *Electrical World*, vol. 48, 29 Sept., 1906, where some experiments are described which corroborate the conclusions arrived at by theory. In the *Journal of the Institution of Electrical Engineers*, vol. 33, p. 1125, the matter is still further elaborated by Mr. M. B. Field, and some practical cases considered. "Eddy-current Losses in Armature Conductors," Girault, *Lumière Elect.*, 4, 35, 1908; "One-sided Distribution of Alternating Current in Slots," Emde, *Elek. und Maschinenbau*, 26, pp. 703, 726, 1908; "Skin Resistance Losses in Alternator Windings," F. Rusch, *Electrotech. u. Maschinenbau*, 28, pp. 73 and 98, 1910; "Eddy-currents in Solid Armature Conductors," Angermann, *Elekt. u. Maschinenbau*, 28, p. 975, 1910; "Copper Losses in A.C. Machines," Rogowski, *Archiv f. Elektrot.*, 2, 81, 1913.

a function of the ratio of the width of the copper to the width of the slot. This ratio Mr. Field denotes by r_1. If a conductor consists of a number of parallel straps one above the other, which are soldered together only at their

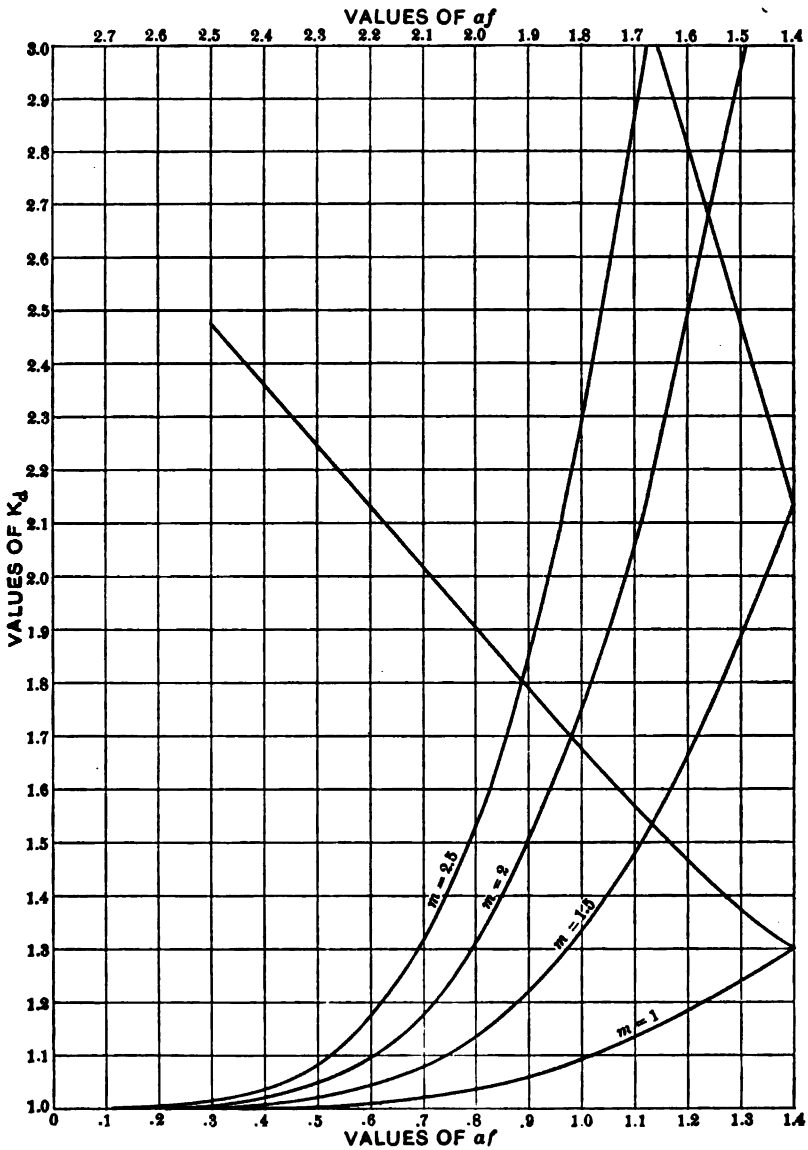


FIG. 167.—Giving ratio K_d of total loss with eddy current to normal loss without eddy current in armature conductors. For use of fractional values of m see pages 119 and 150.

outer ends, then the eddy-current loss is also a function of the ratio of the length of conductor lying in the slot to the total length between the soldered ends. This ratio is denoted by r_2. The eddy-current is also a function of the frequency n.

We will denote the quantity $0{\cdot}145\sqrt{nr_1 \div r_2}$ by a and the depth of the conductor in centimetres by f. Then the values of the product af can easily be found for any case. The ordinates, K_d, of the curve give the ratio of the actual

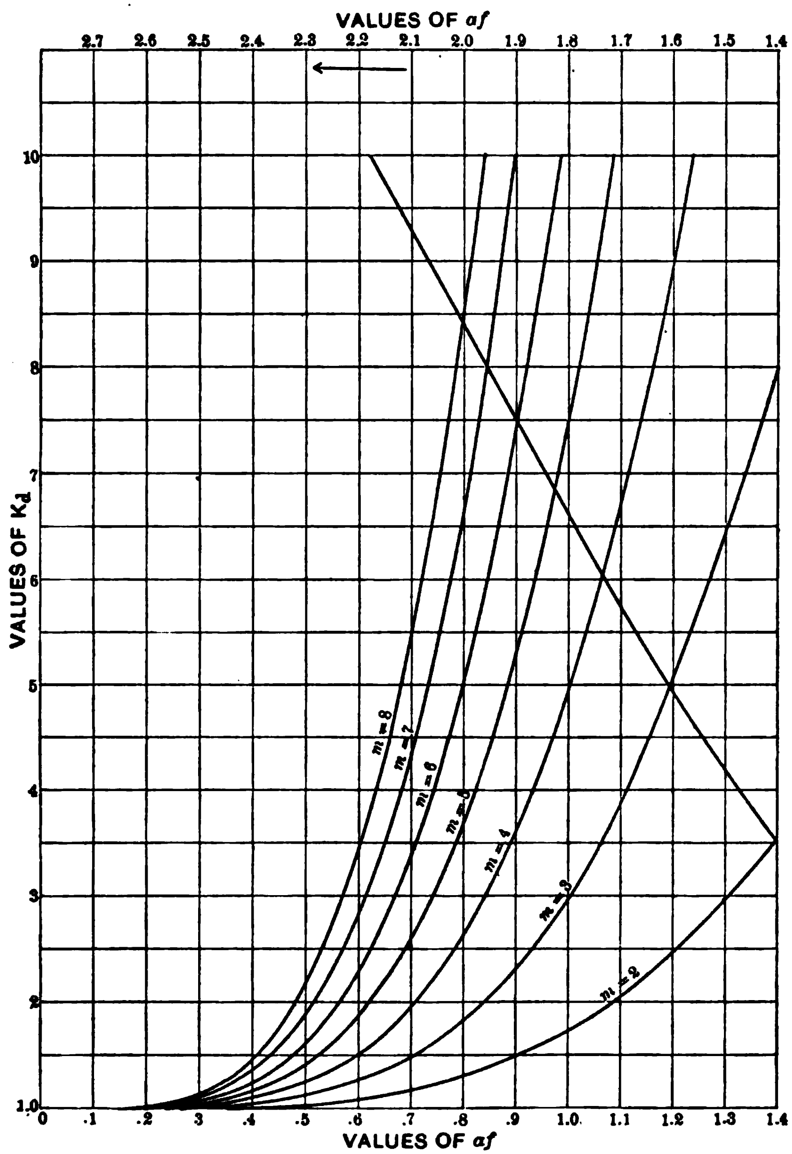


FIG. 167a.—Giving ratio K_d of total loss with eddy current to normal loss without eddy current in armature conductors.

copper loss to the copper loss there would be if there were no eddy current. The abscissae of the curves are the values of af. We give below a few illustrations showing how the curves are employed.

EXAMPLE 25. Suppose that we have a slot ·5″ wide, containing a single conductor ·236″ wide and 1″ deep. As the conductor is solid, r_2 equals 1 and r_1 equals ·47. Suppose that the frequency is 50, then the value of α is ·705 and $f=2·54$. Then the value of $\alpha f=1·79$. We see from the curve that for a value of $\alpha f=1·79$, $K_d=1·67$. That is to say, that with a conductor of this depth working at 50 cycles, owing to the eddy current, the loss is 67 % higher than it would be with a continuous current passing through the bar.

For a frequency of 25 cycles, the value of αf works out at 1·27, from which we find that $K_d=1·23$.

EXAMPLE 26. Fig. 227 shows the conductors of a 25-cycle generator drawn full size. Here $r_1=\frac{·45}{·8125}=·553$. Although each conductor is divided into two parts, these parts are sweated together at no great distance from the end of the slot, so that we may take r_2 as practically unity. We therefore get $\alpha=·145\sqrt{25\times ·553}=·54$. And as $f=1·27$, we get $\alpha f=0·686$. Referring now to Fig. 167, we find K_d

for $m_4=1·88$
for $m_3=1·47$
for $m_2=1·17$
for $m_1=1·02$
4 | 5·54
1·38

Therefore the mean value of K_d is 1·38. That is to say, that on the generator in question the loss in the conductors lying in the slots is 38 % greater than it would be if there were no eddy currents.

EXAMPLE 27. Suppose that we are designing the armature of a 50-cycle turbo-generator, in which it is required to have two conductors per slot, each to carry 700 amps., and that the maximum width of slot permissible is ·925″, and the room required for finish and insulation is ·15″, so that the copper bar cannot be made more than ⅝″ wide. The question arises as to what is the best depth of bar. If we were not concerned with the eddy current, we might begin by assuming a current density of about 1900 amps. per square inch, which would require a bar about ⅝″ × ⅝″. Let us first arrange two bars ⅝″ × ⅝″ with the required space for insulation. It is easy to show from Mr. Field's curves that this arrangement of conductors not only requires more copper than is necessary, but the eddy currents make the total losses in the conductors 35 % more than they would be if the conductors were reduced to the best size. In order to find the best size, it is a good plan to plot the figures proportional to the losses in each conductor for different depths of copper. For this purpose it is convenient to work in centimetres. We have here $\alpha=·145\sqrt{50\times ·675}=·845$. It is convenient to write down the calculated values in columns shown below:

f_1.	αf_1.	K_d.	$\frac{K_d}{f_1}$.	f_2.	αf_2.	K_d.	$\frac{K_d}{f_2}$.
1·6	1·35	1·27	·795	1·6	1·35	3·4	2·12
1·8	1·52	1·395	·773	1·4	1·18	2·39	1·7
2·0	1·69	1·56	·78	1·2	1·01	1·78	1·485
2·2	1·86	1·75	·795	1·0	·845	1·39	1·39
2·4	2·025	1·92	·8	·8	·675	1·16	1·45
2·6	2·2	2·15	·825	·6	·505	1·05	1·75

The reduction on the depth of the top conductor will give more room for copper in the bottom conductor, and therefore, as f_2, the depth in centimetres of the top conductor is reduced as shown by the figures 1·6, 1·4, 1·2, etc., the depth of the bottom conductor is increased as shown by the figures 1·6, 1·8, 2·0, etc. The resistance, apart from eddy currents, is inversely proportional to the depth of the conductor, and so we have divided K by f_1 in order to get a figure proportional to the losses. This is done in the 4th and 8th columns. Plotting the values of these columns, as shown in Fig. 168, we see that the

losses in the bottom conductor are almost constant, while the depth is changed from 1·6 to 2·6 cms., the minimum occurring at 1·8 cms. The losses in the top conductor reach a minimum at about 1 cm. depth. It must not, however, be supposed that this depth is the best, because the cooling conditions on a shallow bar are not as good as on a deeper bar. Moreover, there is always some length of bar outside the slot, in which the eddy-current loss is not so important, and in this part it may be desirable to have a deeper bar (see Fig. 438). In those types of windings in which it is convenient to use a connector larger than the bar in the slot, the value chosen for the depth of the top conductor would be rather greater than 1 cm., say ·4″. This would give a current density of about 2300 amps. per square inch. The value chosen for the depth of the bottom conductor would be about 1·8 cms., say ·7″. This would give a current density of 1620 amps. per square inch. The losses per foot run in the two conductors together, when hot, amount to 45 watts, and as the cooling surface per foot amounts to nearly 45 square inches, we have an allowance of 1 sq. in. per watt, which is quite sufficient for mica and paper insulation more than ⅛″ in thickness (see page 222).

The alternative arrangement would be to use a stranded conductor in the top slot and a solid conductor in the bottom. The stranded conductor could then be made of greater cross-section, say ·7 × ·625, having a total section of ·38 copper. This arrangement would give a lower temperature rise on account of the reduced eddy-current loss in the stranded conductor, a larger section of copper permissible and the greater cooling surface. A stranded conductor, however, is not as strong mechanically as a solid conductor, and is rather more expensive.

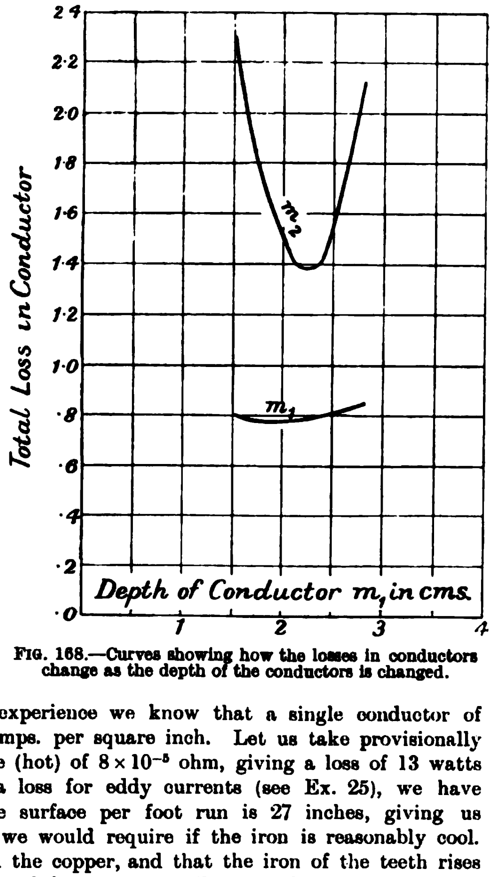


FIG. 168.—Curves showing how the losses in conductors change as the depth of the conductors is changed.

EXAMPLE 28. What is size of copper strap to put in the armature slots of a 3-phase 50-cycle generator running at 1000 R.P.M., if the voltage is 500 and the current 400 amps. per phase? There are to be 144 slots having a pitch of ·7″ and 1 strap per slot. Under these conditions it is not good to use a strap much wider than ⅛th inch, which when insulated will require a slot ·28 wide. From previous experience we know that a single conductor of this kind can be worked at about 3500 amps. per square inch. Let us take provisionally a strap ⅛″ × 1″. This will have a resistance (hot) of 8×10^{-5} ohm, giving a loss of 13 watts per foot run. Allowing about 50% extra loss for eddy currents (see Ex. 25), we have roughly 20 watts. Now the area of the surface per foot run is 27 inches, giving us 1·35 sq. in. per watt. This is more than we would require if the iron is reasonably cool. Suppose that we are allowed 50° C. rise in the copper, and that the iron of the teeth rises only 30° C., then 1·0 sq. in. per watt would be sufficient allowance for cooling. If the efficiency guarantees will permit it, we can reduce the bar to ⅛″ × ¾″, thus somewhat reducing the eddy-current losses. The resistance will now be $1{\cdot}07 \times 10^{-4}$ ohm per foot, giving a loss of 17 watts, and adding 25% for eddy-current losses (see Ex. 25), we have 21·5 watts. The surface of the foot of strap is now 21·3 sq. in., which would be just about sufficient. Note that the strap is now worked as high as 4250 amperes per sq. in. After having arrived at the approximate size by this rough calculation, a more careful calculation may be made of the eddy-current losses. In this case $\alpha = {\cdot}682$, so for a depth of 2·9 cms., $K_d = 1{\cdot}24$. The size of the end connectors would next claim attention. The cooling conditions here depend greatly on the fanning action of the field-magnet, on the amount of insulation on the connectors,

and on the amount of space between them. In any case it is not worth while to work the connectors up to the maximum allowable temperature. A connector $\frac{1}{8}'' \times 1''$ would have a loss of 13 watts per foot run, and as the eddy-current losses would be small there would be about 2 sq. in. per watt. This would be sufficient.

If the machine had 72 slots and were barrel wound (see Fig. 129) with two conductors per slot, the eddy-current loss in the conductor in the top of the slot would have to be seriously considered. The slots could now be about 0·41" wide, allowing room for a bar 0·25" wide. Taking again the provisional value 3500 amperes per sq. in., we would try a bar $\frac{1}{2}''$ deep, and then reducing the bar in the top slot and increasing the bar in the bottom of the slot in successive stages we would find the best cross-section by plotting curves as shown in Fig. 168.

Laminated conductors. Where the conductors are laminated, the laminae lying in the direction of the flux crossing the slot, the eddy current may be reduced. In most laminated windings there are points at which it is necessary to sweat all the laminae together for the purpose of making joints, and the amount of eddy-current loss will depend upon the position of the sweated points with respect to the conductor lying in the slot. For a bar winding with laminated conductors the value of K_d given in the curves applies for the whole length of the conductor between points at which the laminae are connected together. The value r_2 (the ratio between the length of conductor lying in the slot to the length between sweated points) introduced into the formula given above makes the necessary correction to allow for the lamination. For a winding in which the laminae are continued from layer to layer and only joined together at the beginning and end of the coil, we obtain K_d applicable to the whole coil by taking $m = 0{\cdot}5 +$ half the number of layers per slot, if the winding is one in which there are twice as many slots as coils. For the case in which each slot carries parts of two coils, one above the other, we take instead the curve for which $m = 0{\cdot}5 +$ one quarter the number of layers per slot. For a one-layer winding, in which the conductor is twisted over in the middle of the coil so as to reverse the order of the laminae, we take $m = 1$, but refer to a point corresponding to $0{\cdot}5af$ instead of af. The reader should refer to Mr. Field's paper for full information upon these matters.

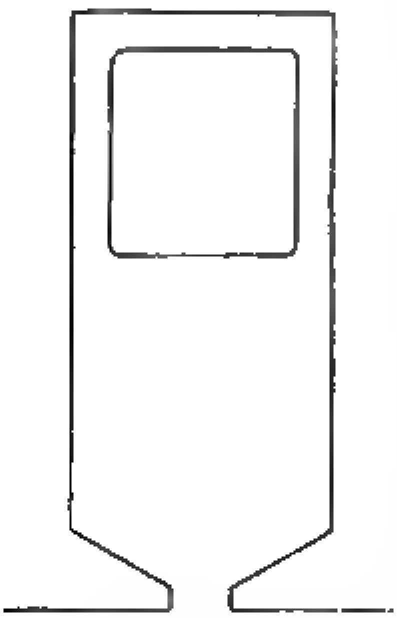


FIG. 168a.—Showing stranded conductor near mouth of slot.

Stranded conductors. The main objection to using stranded conductors is that they are mechanically weak. In cases where the conductors would have heavy eddy currents in them, if solid they should be made with a twisted strand and proper means provided for supporting them. Where two conductors greater than $\frac{1}{2}$ inch are used one above another in a slot on a 50-cycle machine, the losses in the conductor nearest the mouth can be greatly reduced by stranding it as shown in Fig. 168a. The solid conductor can be used to give support to the stranded one.

CHAPTER VII.

THE DESIGN OF ARMATURE COILS AND THE FORMERS UPON WHICH THEY ARE WOUND.

As we have seen on page 89, armature coils may be broadly divided into two classes—(1) Concentric coils; (2) lattice coils. On continuous-current armatures and on all machines in which it is important to preserve a uniform step in phase between one coil and the next, the lattice or overlapping coils are employed. These overlapping coils are of various types.

Armature coils of copper wire may be of the diamond shape shown in Figs. 131, 169, and 170, or of the short type shown in Figs. 133 and 171, or

FIG. 169.—Diamond-shaped armature coils wound with rectangular wire and "pulled" into shape.

FIG. 170.—Diamond-shaped armature coils wound so that the right ends come out at the top and the left ends come out at the bottom.

the involute type shown in Fig. 136. The involute type, however, is now rarely used in rotating armatures.

In Fig. 172, on the left-hand side, we have a single-turn coil of copper strap arranged for two coils per slot. For a multiple-wound C.C. armature this will form part of a lap winding.* On the right-hand side we have a two-turn coil of copper strap, and in Fig. 192 is a strap with the requisite bends in it, made in a bending machine before the coil is formed.

Coils of the short type (Fig. 171) are usually wound in a former or mould. Coils of the diamond type may be either made to the correct shape at once by

* In Fig. 173 we have a strap coil for a series-wound C.C. armature. This will form part of a wave winding.

being wound on a former, or they may be wound in the form of a simple loop, as shown in Fig. 169, and "pulled" to the required shape in a pulling machine (Fig. 177).

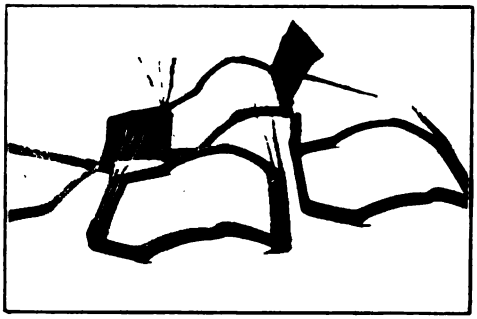


FIG. 171.—Short-type armature coils showing how three "single" sections are grouped to form a "complete" coil, also showing method of insulating by interleaving the sections with black mica-cloth and wrapping it around the complete coil.

Before proceeding with the actual design of the formers, on which the various types of coils are wound, a few remarks are necessary on the former or mould itself and various terms used in the design. The mould, when required for coils of copper wire or ribbon or for field coils of strap wound on the flat, is made of some hard, well-seasoned wood, lined on those parts where the coil is wound with fibre. Fibre is used because it is easily machined to the desired shape, and while it wears well on the mould, it does not injure the cotton or silk covering of the wires. The mould is usually made in two parts, to facilitate the removal of the coil, and sometimes it is necessary to have three or four parts which separate in different planes. Fig. 174 shows a mould for an armature coil, and the way in which the two parts of the mould are separated. One part is fixed to the face-plate of a lathe used in the winding and the other is fixed to the first by means of a pin and tapered key or cotter. The pin goes through both halves, and the cotter, when driven home, holds them firmly together. The sketch (Fig. 193) shows how the wire is taken around the various corners.

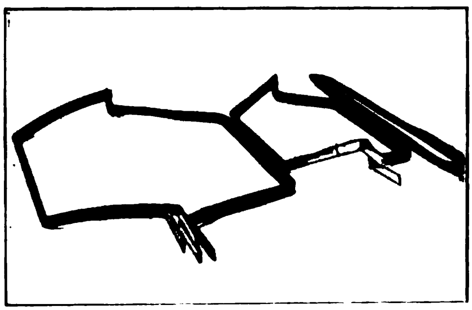


FIG. 172.—Armature coils of copper strap for lap-winding.

The mould for a "pulled" coil is of much simpler design, as can be seen from Fig. 175.

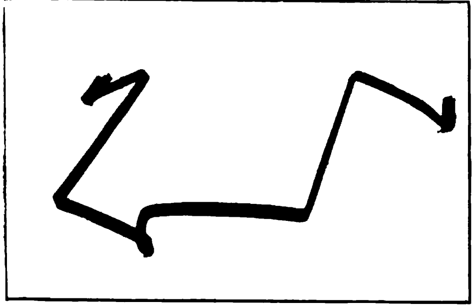


FIG. 173.—Armature coil of copper strap for wave-winding.

Where the armature or stator coils have to be made of stout copper strap, the mould is usually made of cast iron. If only a few coils are required, the mould may be made of wood with iron fittings. Fig. 176 shows a former designed for making coils of copper strap like that depicted in Fig. 173. For this type of former a winding lathe is not required, the straps being simply cut to length

and hammered to shape. Any specially difficult bending, such as bending on edge around a small radius, is done on a bending machine before the strap is put on

FIG. 174.—Mould for a short-type armature coil showing how the two halves are separated.

the former. Formers of this kind are usually made adjustable in length, so as to accommodate coils for different lengths of armature.

FIG. 175.—A mould for a coil which is afterwards to be "pulled" into shape.

In winding the coils on the mould, the wire or strap is hammered into position by means of a mallet and fibre drift. The latter is made of various sizes and

shapes, so as to fit into the crevices of the mould and to level down a number of wires lying together.

In general, the moulds for armature and stator coils are so designed that the coils, when assembled on the machine, will fit together and make a construction which can resist the mechanical forces to which they may be subjected, and at the same time provide for good ventilation.

In case of an armature and stator coil, the slot portion has an extra thick wrapping of insulation to withstand the voltages to ground, and this straight portion of the insulation, commonly called the "cell," projects straight out of the slot a certain distance depending on the voltage of the machine. For machines

FIG. 176.—Cast-iron former for making the type of coil illustrated in Fig. 173.

of voltages up to 500 or 600 volts, the projection of the cell is usually about $\frac{1}{2}$ inch for wire coils and about $\frac{3}{4}$ inch for strap coils, though in very small machines it is sometimes reduced to $\frac{1}{4}$ inch. The amount that the cell projects depends upon a number of considerations. Where the coil is of strap, it is usual to tape the individual straps well round the corner to avoid the risk of short circuits at that point, and the straight cell from the slot is made to overlap the tape; this generally requires about $\frac{3}{4}$ inch of projecting cell. Cotton-covered wires with an insulation of uniform thickness along the entire straight part will not require more than $\frac{1}{2}$ inch of projecting cell. In small machines wound with cotton or silk-covered wires, the projection may be cut down to $\frac{1}{4}$ inch. The amount that the cell projects can only be cut down with safety in armatures which are completely impregnated in a vacuum tank (see Table VIII., page 172).

With regard to the actual winding of the coil on the mould, this may either

be done in "sections" or as a complete coil, depending on the shape. For instance, suppose we have a short-type coil designed to give 30 wires per slot. Half of these will be in the upper limb of one coil and the other half in the lower limb of another coil. That is to say, there will be 15 wires per coil. The bottom half of a coil will be in the bottom of one slot and the top half of the same coil in the top of another slot some way further round the machine, depending on the pole pitch. The winding of each coil, let us say, consists of 3×5 wires, and the mould may be designed so that the coil is wound to shape in 3 "sections," each of 5 wires, which sections are afterwards assembled to form a "complete coil." Fig. 171 shows 3 sections, which are afterwards assembled to form a

FIG. 177.—Pulling machine for making diamond-shaped armature coils.

complete coil. On the right-hand side of the figure is seen the completed coil, and at the back is a coil with the insulation mica and paper tucked around the inside section and ready to be folded around the 3 sections together. The "complete" coil may be wound in the first instance with 15 turns, or 5 turns of 3 wires in parallel, or, as desired, the winding being done in a simple straight mould making a straight coil like that shown in Fig. 169. When the complete coil is wound in the first instance it cannot be conveniently made in a shaped mould. It must be formed after winding. This forming after winding is generally done on a pulling machine, illustrated in Fig. 177. Where the coils are wound in sections, the wires all follow on without any crossing in the section itself; but if it is necessary to connect the sections in series, there will be a cross-over from one section to the next if all the sections are wound the same. Fig. 180 shows a coil with a cross-over. To avoid this, some of the sections

(one-half where there are an even number of sections and less than half where there are an odd number of sections) are wound with the mould reversed on the face of the lathe, so that when the sections are assembled the end of one section comes directly into line with the beginning of the next. The way of assembling two coils (one reversed) to avoid a cross-over is shown in Fig. 180*a*.

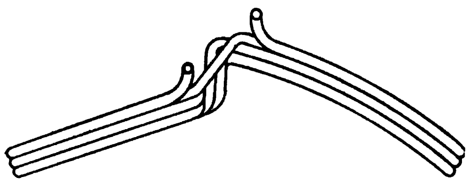


FIG. 180.

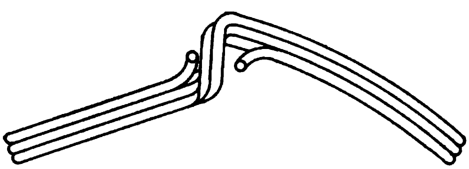


FIG. 180*a*.

The "throw" of the coil is conveniently expressed by giving the numbers of the slots in which the coil lies. Thus we may speak of a throw of 1 and 16. This gives a coil-pitch of 15 slots. In cases where the coil is wound to shape, the mould is split along the throw-line, *i.e.* the line joining the inside top corner of the bottom half of the coil and the inside bottom corner of the top half of the same coil (see Fig. 181). This will be understood more clearly when we deal with an actual example.

Before laying out any stator, rotor, or armature coil, the following particulars must be known, and should be filled in on the design sheet:

(1) Particulars of insulation, its thickness on the ends of the coil and on the slot portion, and also the length that the cells must project from the ends of the slots, the amount that the top cell is to project beyond the bottom one and the minimum distance allowed for electrical or mechanical reasons from the ends of the coils to the nearest metal. This is all covered by the insulation specification.

(2) Bore of stator or diameter of rotor or armature.

(3) Length of iron.

(4) Number of slots.

(5) Size of slots (this is given not as punched size, but as finished size, due allowance having been made for irregularities in the punchings).

(6) Number of coils.

(7) Windings per coil.

(8) Size of wire.

(9) Throw of coil.

(10) Depth of holding-down wedge or band-groove.

The designs worked out here cover the most common cases. The other types of coils in use can be worked out on the same general principles.

The design of a diamond-type coil for the stator of an induction motor. With this type of winding there are as many coils as slots, and all the coils are identical. We shall call that half of the coil that lies in the bottom of the slot "the bottom half" and the part that lies in the top of the *n*th slot, further round on the machine, "the top half." The bottom halves all come straight out of the slots for a certain distance, depending on the voltage, and then bend

round to make such an angle θ with the iron that they all fit closely together and form a surface which is nearly cylindrical (see Fig. 131*a*). The coils then bend up and over and form a second cylindrical surface, in forming which they again fit tightly, and finally they bend to go into the required slots. The design of the coils is usually the same at each end of the machine. For voltages up

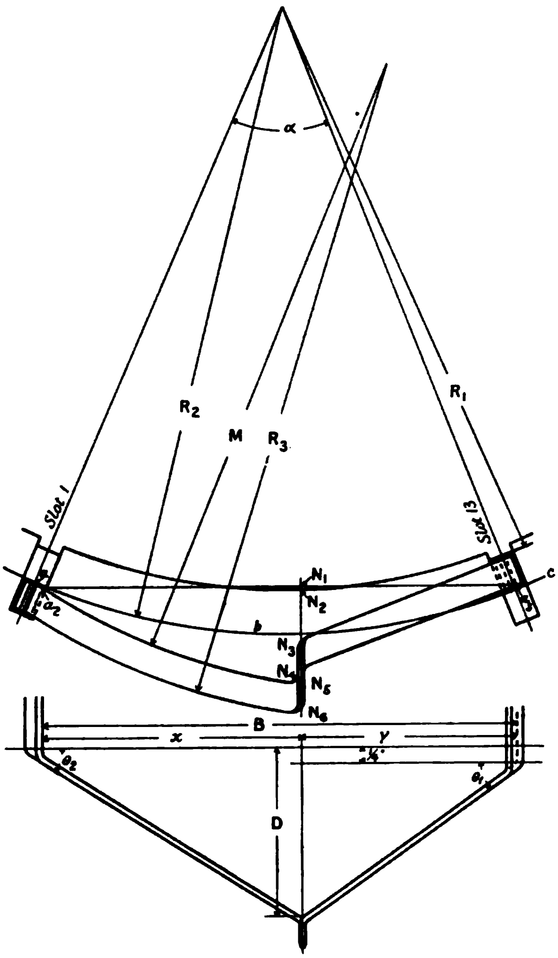


FIG. 181.—Showing the way of laying out the dimensions of the mould for a diamond-shaped coil.

to 500 the coils all carry the same insulation on the ends, but for higher voltages there will be extra insulation on the coils at the end of each phase, where they lie adjacent to the coils of the next phase. An allowance should be made for this in laying out the coils of a high-voltage machine. Ordinary stator coils of this type require no extra support, but if the projection of the coils beyond the slots is very long or if the throw is very long, as in two-pole machines, it is advisable to tie the coils to an insulated metal ring which embraces the whole winding.

The mould worked out below is one on which the coil would be wound to shape in sections. The particulars of the machine are given on design sheet No. 1.

The upper part of Fig. 181 shows the shape of the coil as we look at the stator from the end, and the lower part of the figure as we look at the coil from the centre of the stator towards the outside; the lower figure need not be drawn when laying out the coil.

The coil lies in slots 1 and 13, so the portion of the stator containing these two slots must be drawn in.

Describe first of all a circle (or part of a circle) representing to scale the bore of the stator. The angle α enclosed by the centre lines of the slots in which the coil lies is

$$\frac{13-1}{96} \times 360 = 45^\circ,$$

or generally $\frac{n-1}{N} \times 360$, where N is the total number of slots and 1 and n the throw of the coil. The cord subtended by this angle at the bore of the stator is equal to the bore of the stator multiplied by $\sin \frac{1}{2}\alpha$. Marking off this chord on the bore and drawing lines from the centre through its two ends, we get the centre lines of the slots. The two slots can then be drawn in and also the circle *abc*, where the top and bottom portions of the coil touch, due allowance being made for any packing wedge there may be at the bottom of the slots, and also for the holding-down wedge at the top.

In the present case no packing wedge is required, as the coils just fit the slots nicely. The holding-down wedge is taken as coming $\frac{1}{8}''$ below the top of the slot. Next draw in the slot portions of the coils as a rectangle (the various wires need not be shown), allowing the necessary clearance all around for the insulation specified. Then subdivide this rectangle into as many parts as there are sections in the complete coil. The end view of only one section need be drawn, since all the sections are alike. We will take the one lying in the right-hand bottom part of the slot No. 1 and the right-hand top half of slot No. 13.

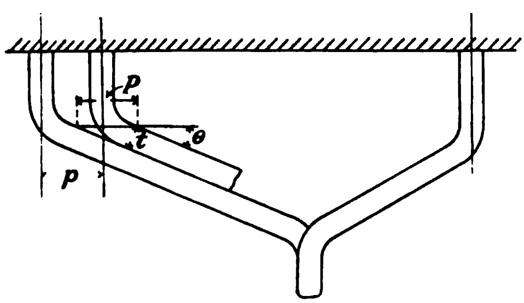


FIG. 182.

Join the points a_2, a_3, *i.e.* the top and bottom inside corners of the bottom and top halves of the section under consideration, by the straight line. This is the "throw line" referred to above.

Turning now to Fig. 182, let t be the thickness of the insulated coil and p the smallest pitch of the slot on the cylindrical surface formed by the coil ends, then θ, the angle which the coil makes with the iron, must satisfy the equation $\sin \theta = \frac{t}{p}$. In laying out the mould in the shops it is not very

convenient to deal with angles, so, instead of specifying θ, we give the lengths x, y and D, and from Fig. 181 we see that $y = B - x$,

$$\text{or} \quad x \tan \theta_2 = D = y \tan \theta_1 + \tfrac{1}{4}''$$
$$= B \tan \theta_1 - x \tan \theta_1 + \tfrac{1}{4}''.$$

Therefore
$$x(\tan \theta_2 + \tan \theta_1) = B \tan \theta_1 + \tfrac{1}{4}''$$
$$\text{or} \quad x = \frac{B \tan \theta_1 + \frac{1}{4}}{\tan \theta_2 + \tan \theta_1}.$$

Therefore, if we find the dimensions x and D and lay the mould out accordingly, the angles θ_1 and θ_2, which the upper and lower coil limbs must make respectively with the iron, will be obtained.

We now require to know the total thickness of insulation on the ends of the coils (exclusive of the cotton covering of the wire), and we will assume in this case that the taping at the ends, including varnish, $= 0{\cdot}06''$ (this is what it would be for a motor up to 500 volts). This insulation allowance is added to the thickness of the cotton-covered wires lying side by side.

Now $\sin \theta_1 = \frac{t}{p_1}$ and $\sin \theta_2 = \frac{t}{p_2}$, p_1 and p_2 being chosen at points along each limb of the coil end which is nearest to the centre of the machine, because there the pitches are smallest, and therefore the angles θ_1 and θ_2 the biggest, and the coils designed to fit at these points will not bind on the parts further from the centre.

On the calculation sheet the pitch p_1 has been taken on the radius R_1 and the pitch p_2 on the radius R_2.

In figuring out the pitch, we have

$$\text{pitch} = \frac{2\pi R}{N},$$

and the rest of the figuring on the calculation sheet is to find x and D.

It has been found advisable in practice, after getting the length $x \tan \theta_2$ or $y \tan \theta_1 + {\cdot}25''$, to add $\cdot 125''$ to the result, so that

$$x \tan \theta_2 + {\cdot}125'' = D = y \tan \theta_1 + {\cdot}375.$$

This $\cdot 125''$ is added to allow for the cutting back of the end and side bevels, and increases the angle θ_1, but not θ_2.

We can now proceed with Fig. 181, having found that

$$x = 4'' \quad \text{and} \quad y = B - x = 7{\cdot}33'' - 4'' = 3{\cdot}33.$$

From a_3 lay off $a_3N_1 = y$ along the throw line, and through N_1 draw a normal to it, cutting the bore circle at N_2. The part of the coil on the right-hand side of this normal must never come nearer to the surface of the iron than $\cdot 125''$, and at the same time the coil should drop as little as is practicable below the iron. It is impossible to wind this part of the coil with a bend in it (as the winding starts on the outside of the curve and progresses inwards, and could not therefore be kept in place), but, on the other hand, if it were made straight, the coil would lie on a tangent and, in many cases, drop further than is necessary below the iron. In such a case a point N_3 is taken on the normal

(0·75″ maximum below the iron), and through this point a line is drawn touching the circle whose radius is R_1. If the shape thus given necessitates a bend in the coil, the bend must be given to it after winding. In the present example N falls just 0·75″ below the iron.

Make N_3N_4 equal to the width of the uninsulated coil and N_4N_5 equal to $\frac{1}{4}$″; this $\frac{1}{4}$″ keeps the two parts of the coil ends apart and enables the coils to lie consecutively without bending at the turn-over. The other limb of the coil is then drawn in through N_5N_6 in arcs of radius M and R_3 struck from the same centre, and the nose of the section can then be drawn with a thickness equal to that of the cotton-covered wire.

Calculation Sheet of Diamond-Shaped Coil.

Mould No. 3241.

Angle between slots $= \frac{13-1}{96} \times 360 = 45°$.

The chord on the bore of the stator $= 18·094 \times \sin \frac{45}{2} = 6·92''$, and from Fig. 181, B (= the length of the throw line) $= 7·33''$.

Thickness of coil on ends (including insulation) $= 3 \times ·093 + ·06 = ·339''$.

$$P_1 \text{ at radius } R_1 = \frac{2\pi R_1}{96} = \frac{2\pi \times 9·17}{96} = 0·601''$$

and

$$P_2 \quad ,, \quad R_2 = \frac{2\pi R_2}{96} = \frac{2\pi \times 9·71}{96} = 0·636''.$$

$$\sin \theta_1 = \frac{·339}{·601} = 0·564'' \quad \text{and} \quad \sin \theta_2 = \frac{·339}{·636} = ·533'',$$

$$\theta_1 = 34·4° \quad \text{and} \quad \theta_2 = 32·3°,$$

$$x = \frac{B \tan \theta_1 + 0·25''}{\tan \theta_2 + \tan \theta_1} = \frac{7·33 \times ·685 + 0·25}{·633 + ·685} = 4·0'',$$

$$y = (7·33 - 4·0) = 3·33,$$

$$D = x \tan \theta_2 + ·125'' = (4 \times ·633) + ·125 = 2·655.$$

We now have all the information we require for filling in the design sheet No. 1, which gives the particulars from which the mould is actually made. A list of these particulars is given below.

Length of cells. The short cell, that is the cell at the bottom of the slot, is equal to the length of iron plus twice the projection of the cell beyond the iron. The long cell, that is the cell at the top of the slot, is equal to the short cell plus twice the distance that the long cell must project beyond the short, according to the insulation specification.

Wire space = number of turns per section × the size of the insulated wire.

A = length of short cell + twice radius F.

B = throw of coil. Sometimes, when the straight limb of the coil is bent after winding, it is advisable to add a little to this, because, after bending, the throw is a little less than before.

$C = x$.

D is obtained as above.

E = length of the long cell over the short cell at each end.

F = the radius put on the corners of the mould to protect the insulation on the wire.

H = the amount added to A at each end, in order that the short cell may be of the proper length, and is usually about $\frac{1}{8}''$. If no allowance were made, this cell could not be made long enough, owing to the intersection of the side bevel with the end of the mould. This can be seen from Fig. 181.

$K = N_1N_4$. $L = N_4N_5$. $M = R_2$.

Bevel N is represented by the rectangular components of a length of the radii bounding the angle α along the throw line and at right angles to it. The bevel on the other side of the mould is the same, but cannot always be made so, owing to the fact that on this side the coil is being wound down the bevel, and if it were made steeper than 1 in 2, would give trouble in winding. It is, therefore, usually made with this amount of slope (unless it actually figures out less), and any extra bevel required can be given by hand to the coil during the operation of putting the coils in the slots.

The mould can now be made from the dimensions given on the mould sheet. The proper thickness of wood and fibre to use is found from actual experience. In the present case the thickness of the mould from back to front would be about 4″ and the fibre lining around which the coil is actually wound $\frac{1}{4}''$.

The mould itself is made in two parts, to facilitate the removal of the section after winding. The split is made along the throw line and parallel to the axis of the machine.

DESIGN SHEET No. 1.

Specification for Armature Mould.

Mould No. 3241.		*6th May,* 1911.
Order No. 78921.	*For Electrical Specification, No.* 632.	
25 *H.P. Motor.*	400 *Volts,* 940 *R.P.M.*	6 *Poles.*
2 *Phase.*	50 *Periods.*	*Insulation Spec.* 1890.

Diameter of Armature, $18\frac{3}{32}''$.

Length of Armature, $7\frac{1}{2}''$.

Number of Slots, 96.

Size of Slots, $\frac{25}{64}'' \times 1\frac{13}{64}''$.

No. of Coils, 96.

Winding per Coil, 3 sections each of 5 wires.

Size of Wire, ·081″ d.c.c.

Coils in Slots, 1 and 13.

Lengths of Cells, $8\frac{1}{2}''$ and 9″.

Wire Space, $\frac{15}{32}''$.

A $8\frac{5}{8}''$.	K $1\frac{1}{2}''$.
B $7\frac{3}{8}''$.	L $\frac{1}{4}''$.
C 4″.	M $9\frac{5}{8}''$.
D $2\frac{5}{8}''$.	N 1 : 2.
E $\frac{1}{4}''$.	
F $\frac{1}{8}''$.	
H $\frac{1}{8}''$.	

NOTE.—Depth of holding-down wedge $\frac{1}{8}''$.

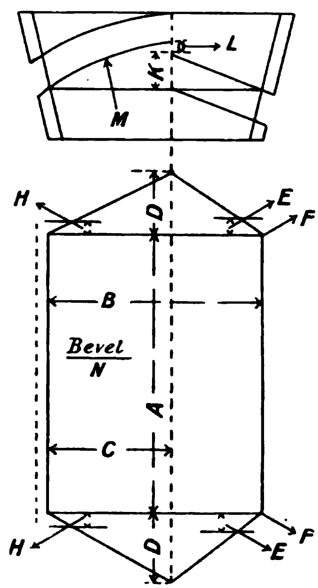


FIG. 183.

A sketch is made (not necessarily to scale) (see Fig. 184) showing the distances that the coil projects beyond the end of the iron and how far it falls below the bore of the iron, so that it may be seen that it does not foul the end bell or any other part of the machine.

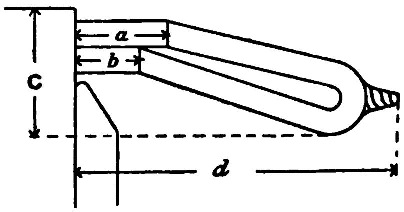


FIG. 184.

a = length of long cell over iron.

b = length of short cell over iron.

c = drop of coil below bore of the iron. This can be measured directly off Fig. 181. A margin (in this case $\frac{1}{4}''$) should be added for safety, as this type of coil can easily be distorted.

d = length of short cell over iron + radius $F + D$ + width of coil + length of stub + a small allowance for safety. The stub caused by the jointing of the wires of the different sections will vary with the size and number of the wires. In some cases it will be possible to bend it over or to get it between the coils.

In the present case $a = 1''$, $b = \frac{3}{4}''$, $c = 2''$, $d = 4\frac{1}{4}''$.

In the case of a diamond-type coil for a revolving armature, slight modifications must be made in the procedure. For instance, the bent limb of the coil will be the one to fall only a short distance below the top of the iron, and the minimum diameter on which the straight limbs fit together can easily be found by trial and error. What has been called the bent limb of the coil can also be wound straight, but the finished appearance of the armature is then not quite so pleasing. Straight limbs build up on a curved surface at the ends. The hollow curve is sometimes useful for preventing a band of steel wire from slipping off.

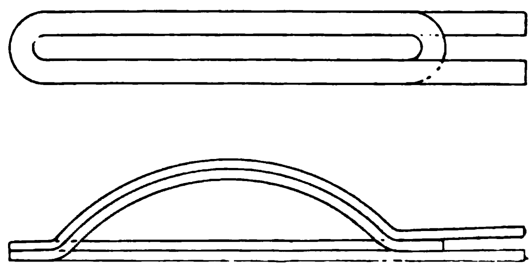


FIG. 192.

In the case of strap coils consisting of two or more turns, the mould is made of iron, and is often adjustable in length by means of a screw (see Fig. 176). On the mould sketch the length of mean turn is given approximately, and this length of copper strap is taken and bent in a bending machine into the shape shown in Fig. 192. As a greater length of copper is required for one side of the coil than for the other, some extra length is allowed, making a sag at one side, as seen in Fig. 192. The strap, then, is put on the mould, a pin going through it at each end. It is next bent roughly to shape over the mould, and the two ends of the mould are screwed apart while the coil is hammered to shape. Any modification in the length which is then found necessary is made and the other coils formed from a suitable length of strap.

In the case of a one-turn strap coil, the copper is bent into a rough U-shaped piece and then hammered to shape on the mould.

Where the coil consists of two or more straps side by side, each consisting of one turn, as is often the case in direct-current machines, the coil is formed to shape, and while on the former, the straps are opened out at the front end

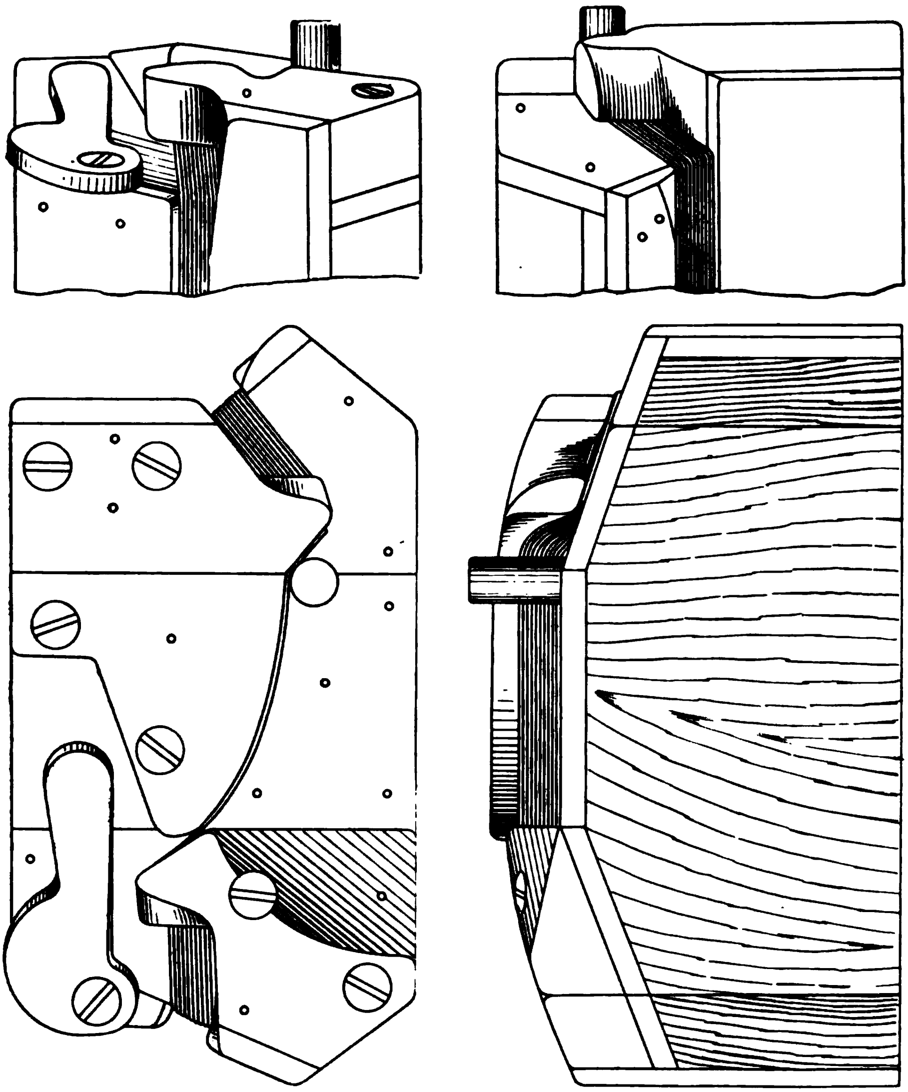


FIG. 193.—Sketch of a mould for a "short-type" coil showing coil in position.

by means of small shaped wedges driven in between, so as to shape them correctly for lying in the right commutator bars.

The design of a short-type coil for a C.C. armature. We will now consider the design of a type of coil which is sometimes called the "short" type, because it does not project as far horizontally beyond the iron as the diamond coil (see Fig. 133). It is often used in direct-current machines where the end

room is limited. It has the further advantage that no coil support is necessary, as the coils, owing to their shape, fit on to one another, and when banded make a good strong mechanical construction. It drops further below the iron than the previous design.

With this type there are as many coils as in the case of the diamond coil, and the coil itself is the same, with this one exception, instead of forming a distinct nose where the coil bends over, the change from the upper to the lower limb is made gradually, this part of the coil having an involute shape. Further, the upper and lower limbs of the coil are further apart than on the diamond coil, and no layer of insulation need be put between them. An armature wound with these "short" type coils is illustrated in Fig. 133, and the coil itself is illustrated in Fig. 171.

Fig. 174 shows a mould for a short-type coil. On each side of the figure are the two halves of the mould separated. Fig. 193 shows how the coil lies in the mould after it has been wound.

Fig. 194 shows the end of a short-type coil consisting of three sections, and Fig. 195 gives the construction by which we can determine the dimensions of the mould upon which a single section of the coil is to be wound.

The involute parts of the short coils are designed so that they all lie together without binding and without having too much room between them. The involute curve is described by a point on a string which is being wound on a circle called the "base" circle. In Fig. 195 the radius of the base circle is r_1. The circumference of the base circle must be equal to tN, where t is the thickness of the coil and N the number of coils on the armature. Thus the radius of the base circle r_1 is equal to $\frac{Nt}{2\pi}$.

This short type of coil must be made in sections, each wound in a mould. The design worked out below is for the armature coil of a 12 H.P., 220 volt, 350 R.P.M., 4-pole motor. We are, in the first place, supplied with the data as to diameter and length of armature, number of slots, etc., given on Design Sheet No. 2.

The first thing to do is to calculate the positions of the two slots in which the coil lies, then draw the "throw" line a_2a_1 and circle abc (Fig. 195) along which the top and bottom halves of the coil touch on leaving the slots. These things are done in exactly the same way as was described on page 158 with reference to diamond coils. It is not necessary to draw in more than one of the sections of a complete coil, because all the sections are the same. Let us take the one lying on the right-hand bottom corner of slot No. 1 and the right-hand top corner of slot No. 10.

We have to determine the positions of the points N and M on the throw line (see Fig. 195), at which normals to the line cut the lower and upper ends of the involute respectively. The edge a_2N in Fig. 195 of the section need not necessarily lie on the throw line, though in the case we have illustrated we have made it do so. If it fell below, the wire would, in winding on the mould, tend to pull down the bevel and make the coil difficult to manufacture. This remark applies to all 4-pole machines, but the larger the number of poles the more N can drop below the throw line without causing inconvenience.

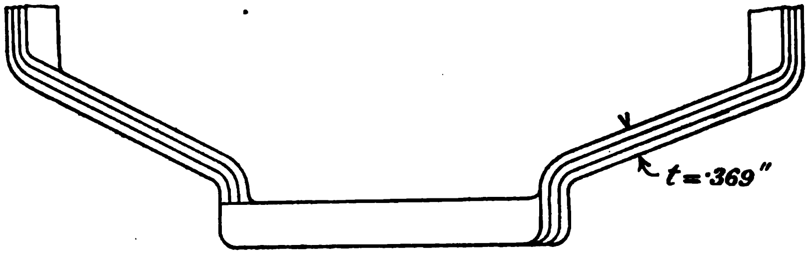


Fig. 194.

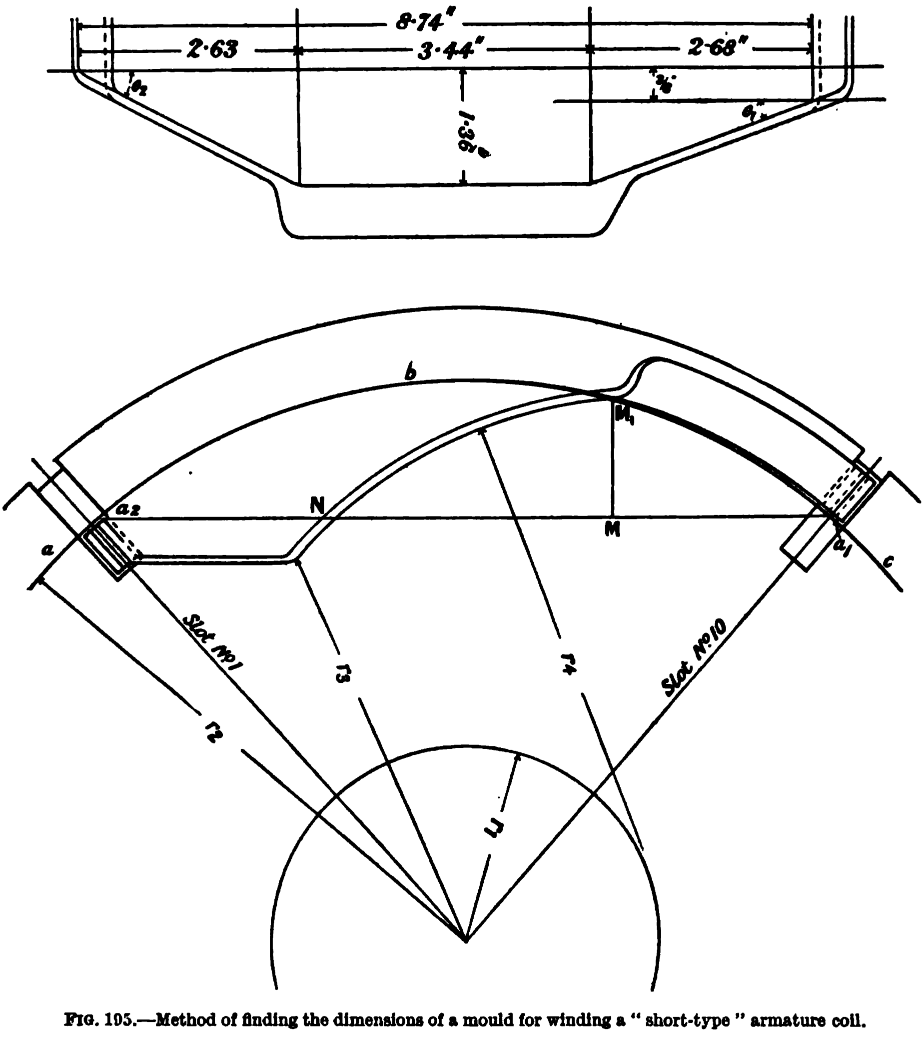


Fig. 195.—Method of finding the dimensions of a mould for winding a " short-type " armature coil.

In fixing the positions of the points M and N, we have to satisfy the equation

$$a_2N \tan \theta_2 = a_1M \tan \theta_1 + \tfrac{1}{4}'' + \tfrac{1}{8}'',$$

The $\frac{1}{4}''$ being the length by which the longer cells has to exceed the shorter, and the $\frac{1}{8}''$ an extra for the cutting back of the end and side bevel.

We can make the involute part of the coil longer or shorter as we like. The further apart M and N lie, the more the coil will drop below the iron, and the

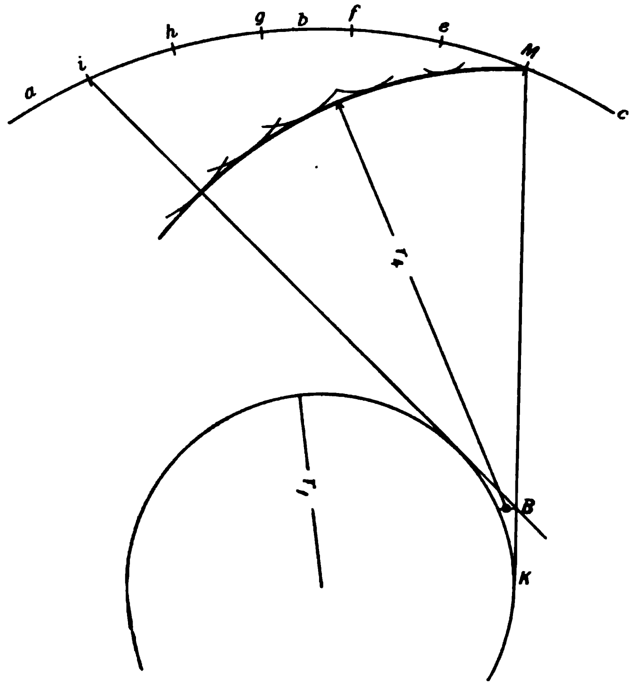


FIG. 196.—Showing construction for finding the centre of the circle nearest to the required involute.

nearer together they are the further the coil will project, until finally, when they coincide, we get a plain diamond coil.

It will be found in practice that there is nothing to be gained by making NM greater than shown in Fig. 195, that is, about 0·4 of the throw line.

It is quite unnecessary to shape the mould exactly to the involute curve, because in any case the coil is flexible and will adapt itself so as to fit in well with the other coils. It is sufficient to shape the mould to the arc of a circle which lies most nearly on the involute. A simple way of fixing the position of M and N is to draw the involute, or the arc that lies near it, on a piece of tracing paper, as shown in Fig. 196. The circle abc and the base circle are the same as in Fig. 195. Along the circumference of abc are set off from M the points e, f, g, etc., at distances, giving the pitch of the slots. At centre e, and with radius t equal to the thickness of the coil, describe the arc of a circle as shown. At centre f, with radius $2t$, describe another small arc. At centre g, with radius $3t$, another, and so on. The required involute will touch these small arcs. If the involute is to be of

no greater extent than shown in Figs. 195 and 196, the circle whose arc lies nearest to it may be found very simply as follows. From M draw a tangent touching the base circle at k. From i draw a tangent cutting Mk at B. With k as centre, draw a small arc from B cutting the base circle. Let the middle point of this small arc be O. Then O will be found to be the centre of the circle which almost touches the small arcs drawn from e, f, g, etc. In practice, therefore, when dealing with only a small length of involute, we may draw the arc of a circle at once, finding the centre by the construction given in Fig. 196.

If the arc drawn on tracing paper is placed over the drawing of the two sloping limbs of the coil (Fig. 195), and pivotted about the centre of the base circle, it is easy to fix M and N so as to satisfy the equation connecting Ma_1 and Na_2, and at the same time make the involute long or short, to suit the room that we have available.

The radius r_3 is the radius of the circle formed by the lowest parts of the coils. This circle must not only be made large enough to clear every part of the shaft, hub, spider and bearing housing, but should also allow sufficient room for the circulation of air.

The plan view of the end of the coil is shown in Fig. 194, but need not actually be drawn.

After the calculations have been made, as given in the calculation sheet below, we can proceed to fill in the mould designs, sheet No. 2. The dimensions A, B, C, etc., will be understood from the sketch, Fig. 197.

DESIGN SHEET No. 2.

Specification of Armature Mould.

Mould No. 3242. — 11*th May*, 1913.
1*st S.O. No.* 62813. — *For Electrical Specification, No.* 521.
12 *H.P. Motor.* — 220 *Volts*, 350 *R.P.M.* — 4 *Poles.*

Diameter of Armature, 15".
Length of Armature, 5".
Number of Slots, 39.
Size of Slots, ·407" × 1·5".
Coils per Slot, 39.
Turns per Coil, 3 sections each of 5 wires.
Size of Wire, ·092" d.c.c.
Coils in Slots, 1 and 10.
Length of Cells, 6" and 6¾".
Wire Space, ·515".

A 6⅛".	*K* ⅜".
B 8·74".	*L* 6·65".
C 2·68".	*M* 1·4".
D 3·44".	*N* 0.
E 2·62".	*P* 5·15".
F 1·36".	*R* 1 : 1⅛.
H $\frac{1}{16}$".	*S* 1 : 2.

NOTES.—Top leads, 14".
Bottom leads, 13½".
Depth of holding-down wedge, $\frac{5}{32}$."

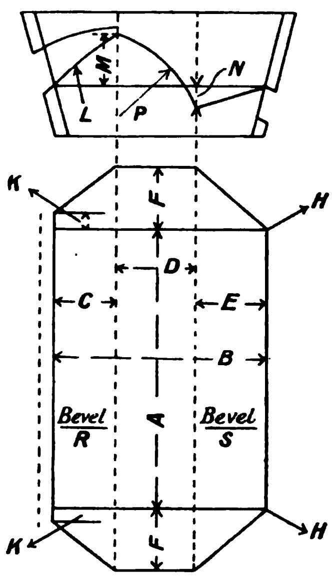


FIG. 197.

A sketch is made (not necessarily to scale) (see Fig. 198) showing the distances that the coil projects beyond the end of the iron, and how far it falls below the working surface of the iron.

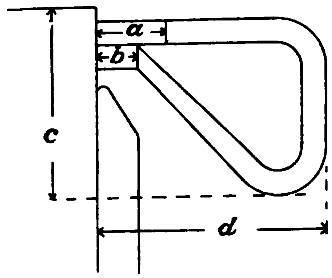


FIG. 198.

Calculation Sheet of Short-Type Coil.

Mould No. 3242.

Angle between slots $=\frac{10-1}{39}\times 360 = 83°$.

Chord on diameter of armature $= 15 \sin \frac{83}{2} = 15 \times {\cdot}662 = 9{\cdot}94''$.

Thickness of coil $= 3 \times {\cdot}103 + {\cdot}06 = 0{\cdot}369''$.

$$r_1 = \frac{39 \times 0{\cdot}369}{2\pi} = 2{\cdot}29,$$

$$a_1a_2 = 8{\cdot}74,$$

$$P_1 \text{ at radius } r_2 = \frac{2\pi \times 6{\cdot}64}{39} = 1{\cdot}07.$$

Therefore $$\sin\theta_1 = \frac{{\cdot}369}{1{\cdot}07} = {\cdot}345,$$

$$\theta_1 = 20{\cdot}2° \quad \text{and} \quad \tan\theta_1 = 0{\cdot}368,$$

$$a_1M \times \tan\theta_1 + {\cdot}375 = 2{\cdot}68 \times {\cdot}368 + {\cdot}375 = 1{\cdot}36'',$$

$$P_2 \text{ at radius } r_3 = \frac{2\pi \times 4{\cdot}96}{39} = 0{\cdot}8.$$

Therefore $$\sin\theta_2 = \frac{{\cdot}369}{{\cdot}8} = {\cdot}461,$$

$$\theta_2 = 27{\cdot}5° \quad \text{and} \quad \tan\theta_2 = {\cdot}52,$$

$$a_2N \times \tan\theta_2 = 2{\cdot}62 \times {\cdot}52 = 1{\cdot}36.$$

FORMERS FOR CONCENTRIC COILS.

Coils which form part of a winding such as illustrated in Figs. 112, 113 and 114 are sometimes spoken of as "concentric" coils. When open slots are used, a coil can be inserted after it is wound and insulated, but when semi-closed slots are used, the coil is only formed at one end, the other end being left open so that the straight limbs can be pushed through the slot and connected up in position.

Fig. 199 shows a number of concentric coils intended to be placed in open slots. Both ends of the coils are "bent up." When a two-tier winding is made with coils that are pushed through semi-closed slots, it is usual to make the ends that are connected up in position to form the part of the winding that projects straight out as in Fig. 113*a* and in Fig. 114.

FIG. 199.—Two views of armature coils of the "concentric" type, made by the Oerlikon Machine Co.

In designing a mould for coils of this type, the first step is to lay out the arcs of the circles upon which the coils will lie and then to set off the pitch of the coils as is done in Fig. 200. The clearances between the coils must be obtained from the insulation specification, due regard being given to the allowance of space for

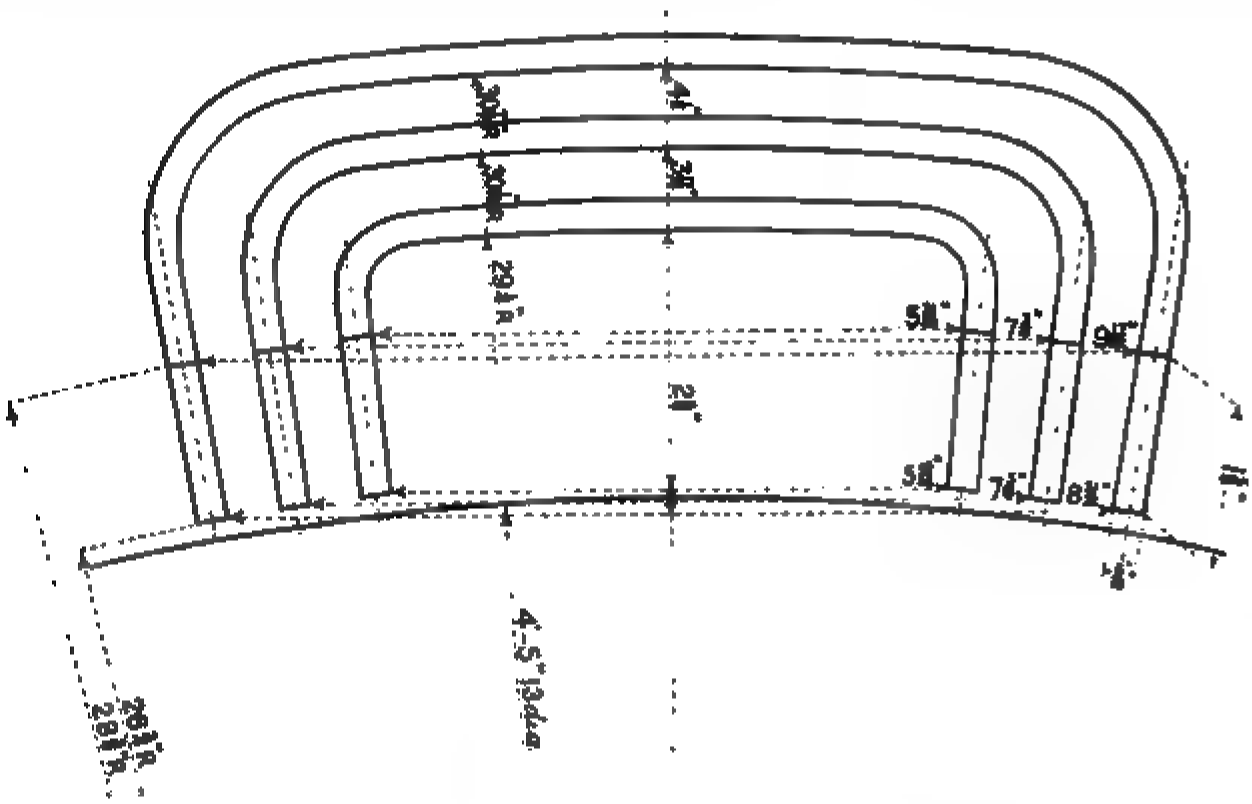


FIG. 200.—Layout of three concentric coils.

air to circulate. From this drawing and from our knowledge of the length of iron and the length of projection of the cells (see page 172), we proceed to make out the coil winding instructions given in Design Sheet No. 3. These instructions relate to the coils of a 450 H.P. 3-phase induction motor, wound for 2000 volts,

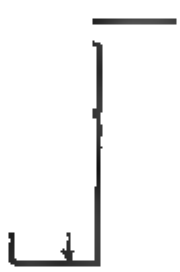


FIG. 201.

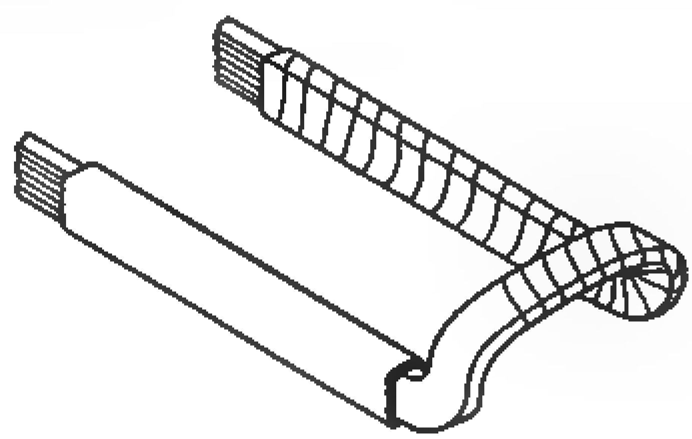


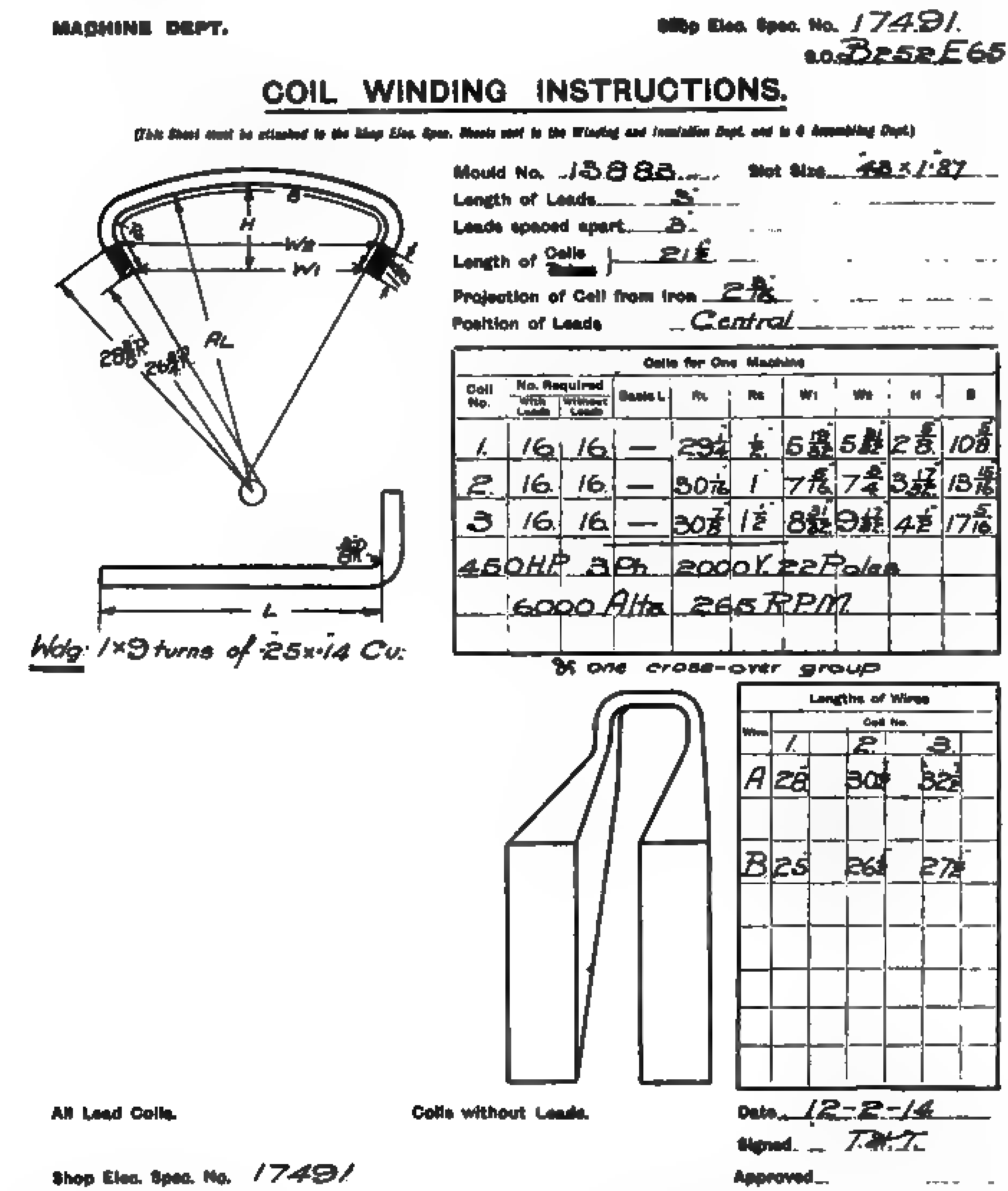
FIG. 202.

having 22 poles. There are 9 slots per pole, and 9 wires per slot. The completed winding is shown in Fig. 114. The bottom portion of the winding instructions gives a diagrammatic view of the coils. The letters *A* and *B* represent lengths which are specially specified, so that all joints in the wires on the straight end are staggered.

In order to settle upon the lengths of wire that will be required for each coil, it is convenient to have a table such as Table VIII., giving the dimensions of the

various parts of the overhang, lettered *A*, *B*, *C* and *D*, on Fig. 201. The dimension *A* is the sparking distance over the surface of the insulation. It really should depend upon the voltage to earth, which in three-phase machines is often less than the voltage between phases. It is, however, good practice to take the voltage to

DESIGN SHEET No. 3.


MACHINE DEPT.

Shop Elec. Spec. No. 17491.

S.O. B252 E65

COIL WINDING INSTRUCTIONS.

Mould No. 13883 Slot Size ·48 × 1·87

Length of Leads 3"

Leads spaced apart 3"

Length of Coils 21"

Projection of Coil from iron $2\frac{5}{16}$

Position of Leads Central

Coils for One Machine									
Coil No.	No. Required With Leads	No. Required Without Leads	Basic L	R_1	R_2	W_1	W_2	H	B
1.	16	16	—	$29\frac{1}{4}$	$\frac{1}{2}$	$6\frac{13}{32}$	$5\frac{[illegible]}{32}$	$2\frac{5}{8}$	$10\frac{5}{8}$
2.	16	16	—	$30\frac{1}{16}$	1	$7\frac{5}{16}$	$7\frac{3}{4}$	$3\frac{17}{32}$	$13\frac{15}{16}$
3	16	16	—	$30\frac{7}{8}$	$1\frac{1}{2}$	$8\frac{21}{32}$	$9\frac{13}{32}$	$4\frac{1}{2}$	$17\frac{5}{16}$
450 HP 3 Ph. 2000 V. 22 Poles									
6000 Alts. 265 R.P.M.									

⅛ one cross-over group

Lengths of Wires			
Wire	Coil No. 1.	2.	3.
A	28	30	32
B	25	26	27

All Lead Coils. Coils without Leads.

Date 12-2-14

Signed T.W.T.

Approved

Shop Elec. Spec. No. 17491

earth as if it were the voltage between phases, as this allows for the accidental earthing of one terminal. In many machines, the insulating tube is put on the straight part of the coil as in Fig. 202, and the end taped over afterwards. In cases where this taping can be impregnated so as to make the insulation to earth over the whole coil strong enough to withstand the full testing pressure, the dimensions *A*, *B*, *C* and *D* can be considerably reduced. But experience shows that

the preservation of these distances is of great service in guarding against accidental weakness in the insulation of the bent parts of the coil. In calculating the length of wire regard must be had to the dimensions x and y shown in Fig. 201.

TABLE VIII. DIMENSIONS OF THE OVERHANG OF CONCENTRIC COILS.

Volts between phases.	*A.*	*B.*	*C* and *D*.
500	1·5 cms.	1	·7
1,100	2·5	1·5	1
2,200	4	2	1·5
3,300	5·5	3	2
5,000	7·5	4	3
6,600	10	5	4
11,000	15	6	5
15,000	20	8	6

FIELD MOULDS.

These require little explanation. As already pointed out, the coil should be designed so that when insulated it either fits tightly on the pole with no air pockets between it and the pole, or so that proper provision is made for the circulation of air between the coil and the pole.

For wire-wound coils (an example of which is given), the mould consists of four pieces, *i.e.* two side cheeks made of wood, and a centre piece on which the coil is wound, consisting of two pieces of wood fitting together at an angle, and covered on the winding surface with fibre. The centre piece is recessed into the cheeks, and the whole bolted to the face plate of the winding lathe.

In the example here given, the pole dimensions are $6'' \times 5\frac{1}{4}''$, and the pole corners are rounded off to a $\frac{1}{4}''$ radius. Assuming that the coil is wound direct on the mould, and then insulated afterwards, and that the insulation between pole and coil is $\frac{3}{32}''$ thick, we then get the size of mould as $6\frac{3}{16}'' \times 5\frac{7}{16}''$. We must, however, allow some clearance, so that the coil will go on the pole without injury, and should therefore allow $\frac{1}{32}''$ at each side, and $\frac{1}{4}''$ at each end. The finished dimensions will then be $6\frac{11}{16}'' \times 5\frac{1}{2}''$. The wire space, or space which the coil itself (uninsulated) can take up radially, must be got from the drawing of the machine, and is in this case $4\frac{1}{2}''$. In getting this dimension from the drawing, allowance must be made for the fact that the coil will spring when removed from the mould (in this case about $\frac{1}{4}''$), and the insulation on the top and bottom of the coil being, say, $\frac{1}{8}''$, the finished depth of coil radially is $5''$.

The dimension C, or height of the cheek above centre piece, depends on the number and size of wires used, and in this case would be made $3\frac{1}{2}''$. The radius D will depend on the size of wire. Design sheet No. 4 gives all the data for a full mould.

Where the coil to be wound consists of strap on the flat, the mould need only have one cheek (fixed to the face plate of the lathe), and a centre piece in which to wind.

For coils wound with strap on edge, a bending machine is necessary to form the corners of the coil, and the coil should be finally shaped on a mould which must be of iron to withstand the necessary hammering.

DESIGN SHEET No. 4.

Shunt Field Mould.

Mould	.	*Superseding Mould*		.	1914.
1st S.O.	.	*For Elec. Spec.*	.	*Ins. Spec.*	.
75 *K.W. Gen.* / *H.P. Motor* }	*ph.*	500 *Volts.*	4 *Poles.*	750 *R.P.M.*	25 *Cycles.*
Frame.					

Length of pole (E.), 6".

Width of pole (F.), 5¼".

Size of {Wire, Strap,} ·081 d.c.c. insd.

Turns of layer, 48.

Number of layers, 34.

Total turns, 1632.

Field Drawing No. 68214.

Wire Space, 4½".

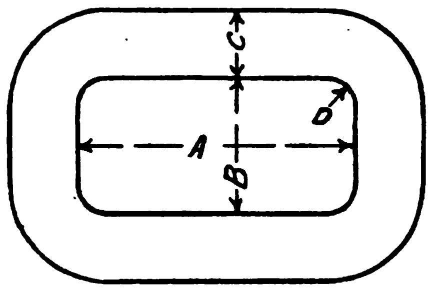
A 6$\frac{11}{16}$". *D* ¼".

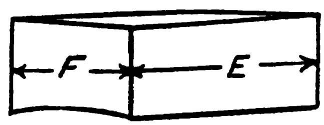
B 5½".

C 3½".

Trial Coils.

NOTES.—





CHAPTER VIII.

INSULATION.

In one sense the design of the insulation is the most important part of the design of a dynamo-electric machine. More money has been lost through the breaking down of insulation in dynamos than through all the other defects in design put together. There is always a tendency for the designer to improve his copper space-factor at the risk of leaving just sufficient space for the insulation. But dearly-bought experience has shown that the insulation should be made as safe as possible, even though we may be compelled to limit the machine in other respects which we may regard as important.

The mere allowing of plenty of room for the insulation is not in itself sufficient, so much depends upon using the right materials in the right places, and in supporting them in a manner which experience has shown to be satisfactory. Even if one type of insulation costs ten times as much as another, it will be found to pay in the long run if the cheaper method has in it any risk either from the mechanical weakness or other defect, for in reckoning the cost we must reckon it as a percentage on the cost of the whole machine, and in estimating the risk of breakdown we must take into account the amount of inconvenience to the user that a breakdown may cause, and the loss of reputation to the manufacturer. Though only one machine may break down among ten, the maker of that machine is widely blamed, while only few people hear of the nine machines that stood up well.

The main qualities of importance in insulating materials are the following:

1. Mechanical qualities.
    - (*a*) Mechanical strength in resisting pressure, tension, bending, bruising, shock and vibration.
    - (*b*) Ease with which material can be worked by being made into sheets, bent into various forms, moulded, and turned and machined into various shapes.
2. Dielectric strength.
3. Specific resistance.
4. Property of being unaffected by moisture.
5. Capability of withstanding high temperatures.

6. Heat conductivity.
7. Property of resisting oxidization and change after a long period.
8. Specific inductive capacity in service.

1. **Mechanical strength.** (*a*) All the good insulators are mechanically weak in some respects. Those that withstand great compressive stresses are weak in tension or lack ductility. Ductility is, of course, an important characteristic of any material which has to withstand mechanical forces; but it is only in the metals (all of which are conductors) that we find some ductility combined with great tensile strength. For withstanding pure compressive stresses, mica is as perfect a material as one could wish for; but if there are any sharp corners limiting the area under pressure, some bending stresses will be exerted on the mica, and there being no ductility, the mica may give way. In the same way, the vitreous and stony insulators, theoretically, can withstand great compressive stresses, but it is difficult in practice to be sure that these are not combined with bending stresses which may bring about breakages. None of the insulators can be relied upon to withstand high tensile stresses. Cotton and linen fabrics are probably the most reliable in this respect, if not baked too dry or treated in a manner which will make them brittle. Cotton and linen fabrics impregnated with flexible varnishes at moderate temperatures form the most flexible insulators known, but their flexibility is destroyed when the material becomes dry (see p. 190).

In the table which is given on pp. 176-177, an attempt is made to state the mechanical qualities of the various insulators and their adaptability for various uses. The attempt is necessarily incomplete, because so much depends upon the quality and state of preservation of the material. For instance, treated cloth, when new, is one of the most flexible insulators known, and is often used in positions where flexibility is important. Nevertheless, treated cloth, if kept for a long time at a temperature of 90° C., will become very brittle indeed.

In order to avoid repetition, and to have a convenient method of referring to the qualities of the materials, we will use certain letters, as given below, to denote the suitability of any material:

Mechanical qualities when in position.

A. To withstand pressure.
B. To sustain tension.
C. To resist deformation when warm.
D. To withstand bending.
E. To withstand shock and vibration.
F. To withstand abrasion.

Mechanical qualities during manufacture.

G. Can be bent in one direction to form angle pieces.
H. Can be bent in two directions to form corner pieces.
I. Can be moulded when hot.
J. Can be moulded in the raw state.
K. Can be machined from the solid.

Table IX. The Qualities

	Mechanical Qualities.											
	Finished material to resist—						During manufacture can be—					Dielectric strength $\sqrt{\text{mean}^2}$ volts per mm. at 50 ~ (see p. 178).
	Pressure.	Tension.	Warmth.	Bending.	Shock.	Abrasion.	Bent one way.	Bent two ways.	Moulded hot.	Moulded raw.	Machined solid.	
Mica	A_1	–	C_1	–	–	–	G_2 (if thin)					15,000 to 40,000
Micanite	A_1	–	C_1 (under pressure)				G_1	H_2	I_1	–	K_2	15,000 to 40,000
Porcelain	A_3	B_3	C_1	–	–	F_1	–	–	–	J_1	–	10,000 to 25,000
Quartz	A_3	B_3	C_1	–	–	F_1	–	–	I_1	–	–	10,000 to 40,000
Marble	A_3	B_3	C_2	–	–	F_1	–	–	–	–	K_1	6,000
Slate	A_3	B_3	C_2	–	–	F_2	–	–	–	–	K_1	3,000
Lava	A_2	B_3	C_2	–	–	F_1	–	–	–	–	K_1	3,000 to 10,000
Glass	A_3	B_3	C_2	–	–	F_2	–	–	I_1	–	K_2	5,000 to 10,000
Asbestos	A_3	B_3	C_1	–	–	–	G_2	H_2	–	J_1	K_2	3,000
Asbestos slate	–	–	C_1	–	–	–	–	–	–	J_1	K_2	1,000
Crystallate	A_2	B_3	–	–	E_2	F_2	–	–	–	J_1	K_1	1,000 to 8,000
Vulcabeston	A_2	B_3	–	–	E_2	F_2	–	–	I_1	J_1	K_1	1,000 to 4,000
Wood boiled in oil	A_2	B_2	–	–	E_1	F_2	–	–	–	–	K_1	2,000 to 8,000
Press-spahn, fuller board or pure paper	A_2	B_3	C_2	D_3	E_2	F_3	G_1	–	–	J_2	K_2	5,000 to 10,000
Do., with one coat of sterling varnish	A_3	B_3	C_2	D_3	E_2	F_3	G_1	–	–	J_2	K_2	20,000 to 30,000
Empire cloth	A_3	–	C_3	D_3	E_2	F_3	G_1	H_1	–	–	–	10,000 to 20,000
Treated tape	A_3	B_2	C_3	D_1	E_2	F_3	G_1	H_1	–	J_1	–	5,000 to 10,000
Cotton covering	A_3	B_2	C_3	D_1	E_2	–	G_1	H_1	–	–	–	3,000 to 5,009
Cotton covering and varnish	A_3	–	C_3	–	E_2	–	G_2	H_2	–	–	–	5,000 to 20,000
Oiled canvas	A_3	B_2	C_3	D_1	E_2	F_3	G_1	H_1	–	–	–	5,000 to 20,000
Leatheroid	A_2	B_2	C_2	–	E_1	F_1	G_1	–	–	–	K_2	5,000 to 10,000
Fibre, white or red	A_2	B_2	C_2	–	E_1	F_1	G_3	–	–	J_1	K_1	1,000 to 10,000
Ebonite	A_4	B_4	–	–	–	F_2	–	–	I_1	J_1	K_1	10,000 to 30,000
India-rubber	A_4	B_4	–	D_1	E_1	F_4	G_1	H_1	–	J_1	–	10,000 to 20,000
Gutta-percha	A_4	–	–	–	E_1	F_2	G_1	H_1	I_1	–	K_2	5,000 to 20,000
Shellac at 28° C.	–	–	–	–	–	–	–	–	I_1	–	–	5,000 to 20,000
Bakelite	A_2	B_2	C_2	–	E_1	F_1	G_1	H_1	–	J_1	K_2	20,000 to 25,000
Paraffin at 46° C.	–	–	–	–	–	–	–	–	I_1	–	–	8,000

of Insulating Materials.

Specific resistance megohms per cm.³ when dry at 25° C. (see p. 189).	Affected by moisture or not.	Heat conductivity at 20° C. (see p. 221).	Safe temperature ° C.	Resistance to oxidization and change with time.	Specific inductive capacity.
5 to 100 × 10^6	Not	·00087	500 or more	Very good	6 to 8
10 to 6,000 × 10^6	Not	·00029	130, if under pressure	,,	6 to 8
1 to 1,000 × 10^6	Not if vitreous	·007	May crack	,,	4 to 5
1 × 10^7 to infinity	Not	·006	500 or more	,,	4·5
400	Affected	·002	May crack	,,	8
40	Affected	·0022	,,	,,	—
400	Affected	·0002	500 or more	,,	—
5 × 10^6 to infinity	On surface	·0002	May crack	,,	5 to 10
16 × 10^4	Affected	·00005	500 or more	,,	—
—	Affected	·0001	,,	,,	—
16 × 10^5	Partly	—	—	Good	—
3 × 10^5	Partly	—	—	,,	—
1,000 to 10^5	Affected	·0004	—	—	—
1,000 to 10^8 depending on the dryness.		·0004	90	Becomes brittle	2
		·0005	90	,,	,,
		·0006	90	,,	,,
		·00035	90	,,	,,
		·00025	90	,,	—
		·0005	90	,,	—
		·0002	90	,,	—
		·0005	90	,,	—
		·0005	90	,,	—
·2 to 100 × 10^8	Slightly	·0004	40	Are destroyed in the presence of air and light	2·5
2 to 10 × 10^8	Slightly	·0004	40		2·2 to 2·5
25 to 5 × 10^8	Not	·0004	40		3·3 to 4·9
9 × 10^9	Affected, unless vitreous	—	Softens at 60	—	3
—	Not	·0006	200	Very good	—
3 × 10^{10}	Not	·0002	Softens at 50 Melts at 55 Boils at 370	—	2

As these materials possess the above properties to a greater or smaller extent, we have attached subscript numbers to the letters, to indicate the suitability of the material for the purpose under consideration. For instance, C_1 means that the material does not soften or deteriorate at all when exposed to warmth. C_2 means that it withstands warmth fairly well, C_3 means that it withstands warmth only moderately well.

(*b*) Most materials which can be moulded into suitable shape when hot, such as gutta-percha, shellac and bitumen, and petroleum residue, have the drawback that they will not resist distorting forces when subjected to a temperature above 50° or 60° C. Some of them can be usefully employed for the impregnation of more solid materials. Ebonite can only be used in places where the temperature is low. Bitumen, rubber, shellac and resinous materials are sometimes mixed with solids, such as asbestos and mica, to form an insulating material which is mechanically stronger, but inasmuch as these materials can be moulded when hot, they will give way slowly if put in warm positions. Some of these can, however, withstand compressive stresses for any length of time.

Within the last few years a new insulating material named **Bakelite** has been introduced. This material, which is supplied in the raw state as a thin, varnish-like liquid, sets under the action of heat and chemical combination to a hard amber-like substance of great insulating strength and good mechanical qualities. It can be used for cementing together layers of paper or asbestos, the resulting product having very fine mechanical qualities, and resisting very well moisture and fairly high temperatures. Bakelite is a combination of formaldehyde and phenol; it resists the action of weak acids and alkalies and a temperature of 250° C., but is affected by strong alkalies. When being heated in the course of manufacture, it must be subjected to a pressure of about 180 lbs. per square inch, otherwise gases evolved in the course of setting will cause it to be spongy.

Where surfaces of a complex shape are to be covered with an insulator, a common method is to wind cotton or linen tape over them, and treat this tape in position with Sterling varnish or impregnate it with bitumen, or with one of the compounds of petroleum residue and bitumen. This mixture of cotton fabric and insulating compound forms a material having a certain amount of ductility and ability of resisting tensile and compressive stresses.

Where a material is supplied in the form of sheets, such as the papers, it can be bent up into various useful forms possessing good mechanical qualities.

The insulating materials which can be cut into suitable shapes from solid blocks are commonly brittle. A good exception to this is hard fibre, which possesses many good mechanical qualities, but is treacherous as an insulator.

Porcelain and stoneware moulded as a clay and baked at high temperature can be used in many cases where a moulded material is required to withstand mechanical forces.

2. **Dielectric strength.** In Table IX. will be found the voltages which various materials of 1 mm. in thickness will withstand. No very definite figure can be ascribed to any particular material, because different conditions in the

application of voltage and the slight differences in material give such wide differences in the result. For materials of perfectly defined composition, and of crystalline form, such as white mica, we could obtain definite figures for dielectric strength if the surrounding temperature and form of terminals were prescribed, and if the time of application of the voltage and other matters were kept constant; but in other materials such as cellulose (in its various forms in cotton cloth and paper, treated and untreated), which may have more or less traces of moisture in their composition, we can hardly expect to get constant figures for the breakdown voltage.

It is really necessary to enquire into what happens when the material breaks down. Where a material such as mica, glass or porcelain breaks down instantly under the application of a very high voltage, it appears as if the breakdown were due to disruption of the molecules under the electric stresses. A hole is pierced through the material, due apparently to the movement of the material along the path where the electric current has passed. In some cases it appears as if an explosion had occurred within the material, and for the instant the forces of cohesion had been inoperative, or had, at least, been overcome by some other much greater mechanical force. Where, however, the voltage is not sufficient to bring about this instantaneous disruption of the material, a breakdown may occur due to the heating of the material by electric conduction through it, and sometimes this heating effect can be produced so quickly as to seem almost instantaneous in action. The breakdown of the cellulose insulators is nearly always due to this heating effect. We can in many cases detect the heating effect by the discoloration of the material if we take off the pressure and examine the material just before a breakdown occurs. Sometimes a material will withstand a high pressure for a few seconds and then break down. Upon the application of the voltage, an electric current (it may be a very minute electric current) passes through the material. This current causes a slight rise in temperature, the rise in temperature increases the conductivity, and as the current increases, the heating increases in greater ratio. With the increase in temperature, the conductivity still further increases and the temperature rises more and more until burning sets in, and a puncture occurs. This is what most commonly happens where treated paper, treated cloth and other cellulose materials break down. It will be seen that for punctures of this character, the voltage which must be applied to effect a breakdown is largely dependent upon the cooling conditions. Taking the material at the temperature of the surrounding atmosphere, a certain current will flow upon the application of a certain voltage. If now the cooling conditions are such that the whole of the heat generated in the material can be conducted away and dissipated without the temperature rising more than a few degrees, the final value reached by the current will not be very great. If at any time heat is being generated in the material at a greater rate than it is being conducted away, the temperature will rise until a point is reached at which the heat dissipated is equal to the heat generated. Such a point can of course only be reached if the rate of increase of loss with temperature is less than the rate of increase of dissipation of heat with temperature.

This matter will be more clearly understood by reference to Fig. 206, in which temperature is plotted as abscissae, and the losses as ordinates. Let curve W represent the watts converted into heat in a given piece of insulation when subjected to a certain voltage, and let curve C represent the rate at which heat is conducted away at different temperatures. The curve C may be taken for our present purposes as a straight line. The slope of this line will be steep if the cooling conditions are good, and small if the cooling conditions are bad. Under normal conditions found in practice, the curve C cuts the curve W at two points, as shown in Fig. 206. The ordinates of curve W will increase very slowly at low temperatures and more quickly at high temperatures. Let t_1 represent the temperature of the surrounding atmosphere;

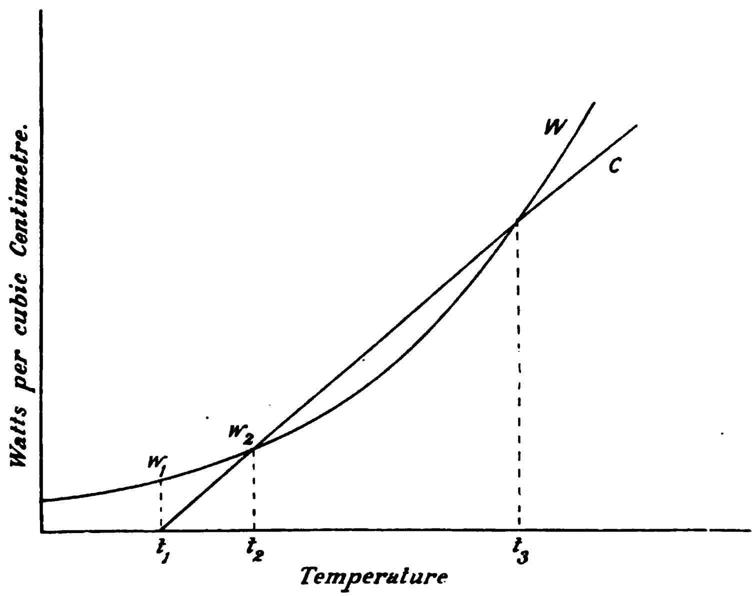


FIG. 206.

the losses in the insulation are, at that temperature, w_1. Under these conditions the temperature will go on rising to t_2, at which point the rate of generation of heat is equal to the rate of dissipation of heat. If from any accidental cause the temperature should be raised a little above t_2, as soon as the cause is removed it will tend to fall back to t_2, as long as the rate of dissipation of heat is greater than the rate at which heat is being produced. If, however, the temperature were raised above the point t_3, where the rate at which heat is being produced is greater than the rate at which heat is being dissipated, the temperature will go on rising and rising until the material is burnt and breaks down. The shape of curve W will depend upon the voltage applied to the insulation, and on the amount of moisture present.

In Fig. 207 are plotted curves which give for different temperatures the loss per cubic inch in well dried rope paper treated with copal varnish when subjected to an alternating E.M.F. at a frequency of 50 cycles per second. The curves are

plotted from the results of experiments* made on a pad, consisting of treated rope paper built up to a thickness of $\frac{1}{4}$ inch, and placed in an oven between two copper plates 9″ in diameter, having well-rounded edges. The voltage was applied between the two copper plates, while the oven could be maintained at any required temperature by means of an electric heater and a circulating fan. The losses were measured by means of an electrostatic wattmeter. It was found that the losses were approximately proportional to the square of the voltage so long as the temperature was constant, but a higher voltage, if

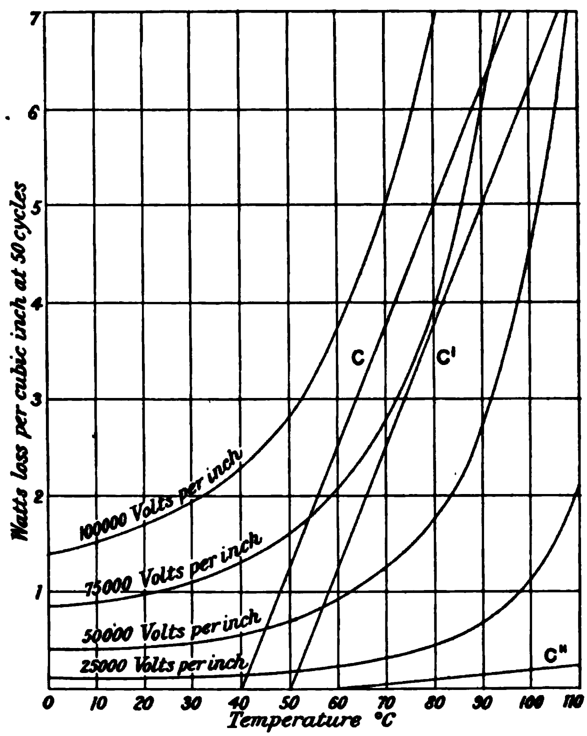


FIG. 207.—Watts lost in well-dried rope paper treated with sterling varnish when subjected to an alternating electric pressure at 50 cycles.

continually applied, would produce a higher temperature and that again a higher loss.

The curve marked 100,000 volts per inch represents the results obtained from applying 25,000 volts to the $\frac{1}{4}$ inch pad. If 100,000 volts had been applied to a 1 inch pad, the centre of the pad would very soon have become hot, and the losses very much increased on account of the bad conductivity for heat offered by a thick pad of paper.

What goes on when an excessively high voltage is applied to paper insulation can be best described by taking an example. Suppose that the conductors in

*C. E. Skinner, "Energy Loss in Commercial Insulating Materials," *Amer. Inst. Elec. Engineers*, vol. 19, p. 1047 (1902).

Fig. 227 are insulated with a $\frac{1}{4}$ inch thickness of paper or some dielectric having the characteristics given in Fig. 207. Assume that the iron surrounding the insulation is maintained at 40° C., and that the cooling conditions of the inside of the insulation are represented by the line *C*. That is to say, when the temperature of the inside of the insulation is 32° C. above the temperature of the iron, the heat passes to the iron at the rate of 1 watt per square inch, or as the insulation is only one quarter of an inch thick, heat passes to the iron at the rate of 4 watts per cubic inch of insulation. If we apply 6250 volts alternating at 50 cycles per second between the conductors and the iron (giving us 25,000 volts per inch of thickness), we would find that the paper would only rise in temperature about 1° C., because with 1 degree rise under the cooling conditions assumed, the rate of loss of heat would equal the rate at which heat was being generated.

With 50,000 volts per inch the temperature would rise about 4·7 as seen from the point where the line *C* crosses the curve 50,000. With 75,000 volts per inch applied, the temperature would rise to 54 degrees or 14 degrees above the surrounding iron. With 75,000 volts per inch, the temperature would not rise above 54° C. so long as the iron remained at 40° C. If, however, the temperature of the iron rose at 50° C. and the cooling conditions were then represented by the line *C*′, the temperature of the insulation subjected to a pressure of 75,000 volts per inch would go on rising until it reached the burning point, because the curve 75,000 does not cross the curve *C*′. Similarly, if the iron were maintained at 40° C. and the voltage raised to 100,000 volts per inch, the paper would certainly break down in time, because the curve 100,000 does not cross the line *C*. We can imagine a case in which the cooling conditions are very poor indeed, say with a surrounding temperature of 60° C., and a conductivity so bad as to allow a dissipation of only one quarter of a watt for 50° C. rise (cooling conditions represented by the line *C*″), then the insulation could be broken down by the application of a comparatively small voltage per inch. In fact, we say that if the heat generated in insulation could be entirely prevented from getting away, any voltage however small could break down any insulation however thick.

This matter is very well illustrated by tests* carried out by Mr. E. H. Rayner at the National Physical Laboratory. Some of these tests were carried out on insulating tubes made of manilla paper cemented with shellac in an oven in which the temperature of the air could be measured. The loss on the insulation when subjected to high alternating stresses was measured by means of an electrostatic wattmeter. The tubes had an external diameter of 23·5 mms., and the thickness of the wall was 1·9 mms. For the purpose of the experiment, the tube was about 50 cms. long, and was sealed with a cork at the lower end. A loosely fitting brass tube sealed also at the lower end was placed inside, and the annular space between the two was filled with mercury, a great weight of which was by this means avoided. A thermometer reading to 0·2°, fixed in a cork, registered the temperature of the air in the inner brass tube. The circumference of the outside of the tube was

* *Journal Inst. Elec. Engineers*, vol. 49, page 3. Figs. 208, 210 and 211 are reproduced from Mr. Rayner's paper.

7·4 cms., and a length of 35·5 cms. was covered with tinfoil. This formed the outer conductor, which had an area of 260 sq. cms.

A series of experiments was carried out to investigate more accurately the effect of changes of temperature, voltage and frequency on material of this nature. They were all done without moving the specimen, which was kept in the oven at a steady temperature, generally at about 24·6° C. The experiments were lettered *A* to *W* in chronological order.

The upper curves *A* to *G* (Fig. 208) show the result of a series of experiments in which 5000 volts, 50 cycles, was applied repeatedly to the same specimen. When first the voltage was applied, the loss in the specimen was only about 4 watts. This loss was sufficient to slowly increase the temperature inside the insulation and increase the conductivity. At the end of thirty minutes the watts had increased by more than 50 per cent., and the rate of increase of temperature was correspondingly greater. In another fifty-five minutes the rate of increase of temperature was so great that the loss curve became almost perpendicular, and if the voltage had not been switched off, the tube would have broken down in the course of a few minutes. This being then allowed to cool down somewhat, the voltage was applied again. This time the loss followed the curve *B*. The quicker rise in the loss was probably due to the initial temperature of the insulation and the brass tube inside it. These curves are characteristic of the behaviour of cellulose insulation when subjected to a high voltage, and it is probable that before a breakdown the loss always increases in the manner shown, and brings about the burning of the material. In all curves *A* to *G* the rate of the production of heat was greater than the rate of dissipation of heat.

FIG. 208.

Fig. 208 also shows the behaviour of the material under a pressure of 4000 volts. Here the rate of increase of loss with temperature was not greater than the rate of increase of dissipation of heat with temperature, and the curve for 4000 volts consequently does not go on rising, but shows a tendency to reach the steady state, where the rate of production of heat is just equal to the rate of dissipation of heat. At a lower temperature, 14·8° C., the losses are lower and the curve is of the same character. At 3000 volts, the loss is reduced, being proportional to the square of the voltage where the temperature remains constant.

The total volume of the material under test was about 49 cu. cms., and the loss per cubic centimetre seems to have varied with change of voltage and change of temperature in the way indicated in Fig. 209. When the material was placed in an oven maintained at 24·6° C., the cooling conditions are represented fairly closely by the straight line *CC′*. At 5000 volts the rate of generation of heat was for all temperatures greater than the rate of dissipation of heat. The 4000-volt curve, however, falls below the cooling condition line, so that the material assumed a steady state at a little over 0·06 watt per cu. cm. The 4500-volt curve almost

touches the cooling line, but not quite. It was found that at 50 cycles a pressure of 4500 appeared at first as if it were going to give steady conditions; but after eighty minutes an inflexion appeared in the curve, and then the watts lost rapidly increased. This is shown in Fig. 210. When, however, the frequency was dropped

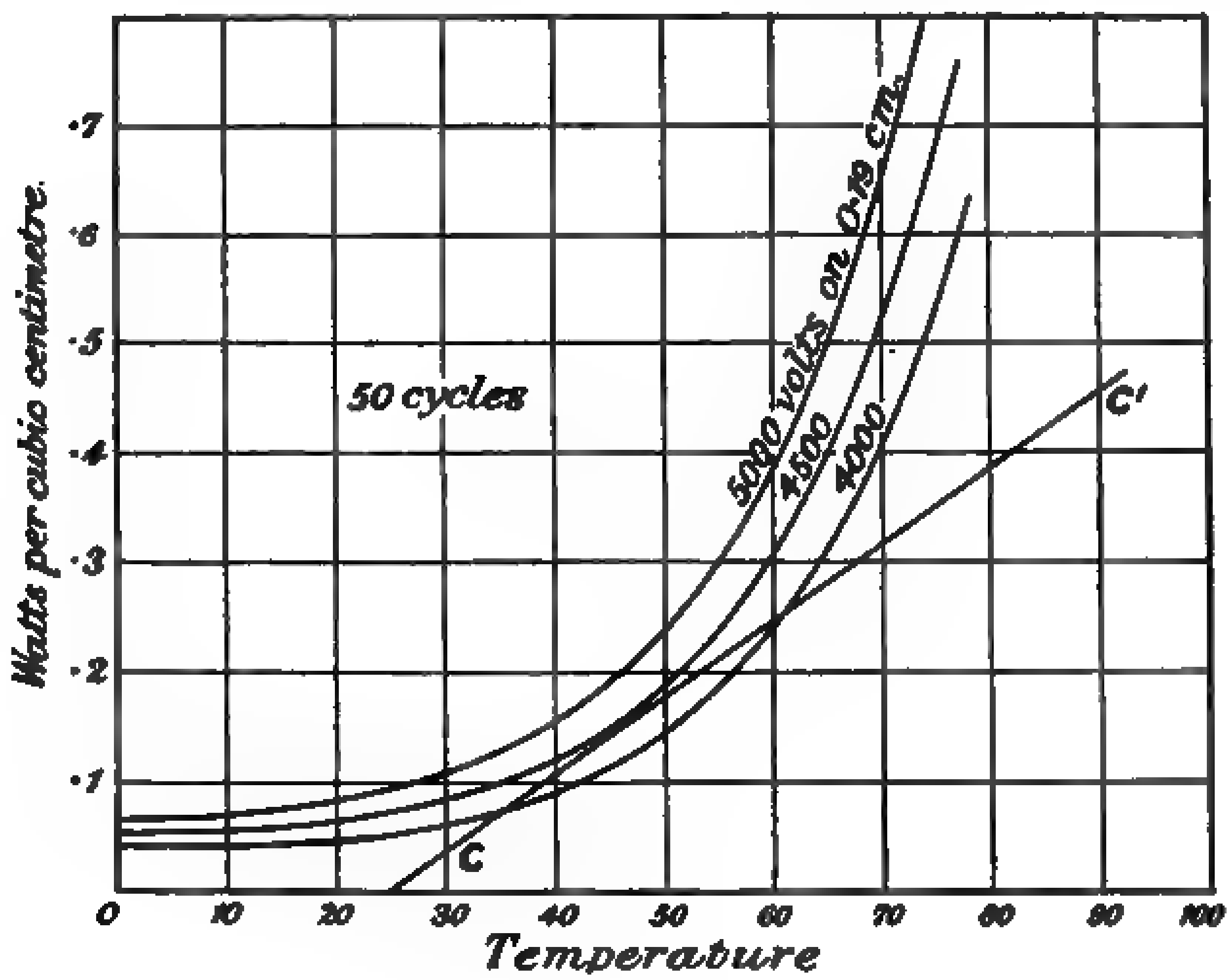


Fig. 209.

from 50 to 47 cycles, the 4500-volt curve was brought down just enough to touch the cooling line, and the conditions, became steady at 0·14 watts per cu. cm. A further reduction of the frequency to 38 cycles made the conditions perfectly stable, as shown by curve N. After the conditions had become very nearly steady, the frequency was raised to 50, and after a few minutes to 56.

The effect of changing the surrounding temperature is shown by Mr. Rayner. With an oven temperature of 50° C., so great was the increase of the loss that a voltage of only 2250 gave unstable conditions, and would have broken down the tube in less than eighty minutes. When the voltage applied is such as to give a curve which very nearly touches the cooling line, it is found

Fig. 210.

that a very small change in conditions makes a very great difference in the behaviour of the material. As seen from Fig. 210, a change in the frequency

from 50 to 53 is sufficient to make the great difference in steepness seen between curves J and K. In Fig. 211 is seen the effect of a slight increase in the voltage. At 4500 volts we have seen the conditions are so near to reaching stability that it takes seventy minutes to reach the point of inflexion of the curve. An increase of the voltage to 4600 volts makes the curve rise more quickly, and the point of inflexion is reached in forty minutes.

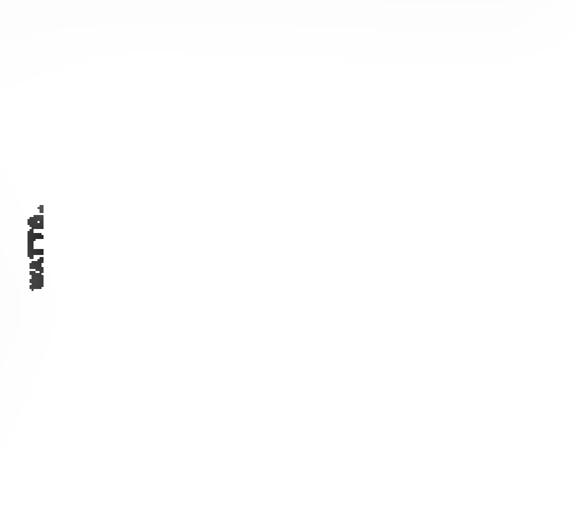


FIG. 211.

The dependence of the breakdown voltage on the cooling conditions is a matter of great importance. A paper only ·007″ thick that will withstand 7000 volts when placed between two cool copper plates will not withstand 25,000 volts when 0·25 inch thick if the surrounding temperature is high and the cooling conditions otherwise bad. In one case the paper withstands 1,000,000 volts per inch, and in the other case it will not withstand 100,000 volts per inch. There are of course other reasons why thick pieces of insulation do not withstand as high a voltage per inch as thin pieces. The potential gradient is seldom uniform in a thick piece of insulation. Very often there are corners producing a steep potential gradient where the lines of electric force radiate from some edge of metal, or if there are no corners there are often differences in the specific inductive capacity or differences in the insulation resistance on different parts causing undue stress to be thrown on some particular part. If there is a brush-discharge from a metal corner, this sometimes heats up the insulation and causes a breakdown.

One of the reasons why mica, whether as micanite or as commonly used interleaved with paper or cloth, resists such high voltages per inch of thickness is that the loss occurring in mica when subjected to an alternating pressure is much smaller than in the case of cellulose.

It should be observed that the losses in a dielectric when subjected to an alternating voltage are much greater than when the voltage is steady. This has sometimes been attributed to Dielectric Hysteresis. But the analogy with the hysteresis in iron is not complete.* For a stick insulation will not retain its static strain for an indefinite period after the surface has been discharged. If we found that it did and that it required the application of a definite voltage to get rid of the electrification, then we would have a perfect analogy. It appears rather that the loss in a dielectric subjected to an alternating voltage is purely ohmic. The material, when the pressure is first applied, allows a dielectric current to pass, by reason of its specific inductive capacity. The amount of electricity which will flow into a condenser made of paper or other impure dielectric depends somewhat on the time that the steady pressure is applied. More electricity will flow into the condenser in two one-thousandths of a second than will flow into it in one-thousandth, though very little more

* J. A. Fleming and G. B. Dyke, "On the Power Factor and Conductivity of Dielectrics . . . ," *Journ. Inst. Elect. Engrs.*, vol. 49, page 323.

will flow into it in two seconds than will flow into it in one second. The material behaves somewhat as if it had ohmic resistance combined with its specific inductive capacity. It thus comes about, that when we charge and then discharge the condenser we have suffered a certain ohmic loss. For low frequencies the ohmic loss per cycle is constant, so that the loss per second is proportional to the number of cycles per second. But when the frequency is of the order of 100 cycles, the loss per cycle is less, owing no doubt to the fact that the condenser has not time to get its full charge. We therefore find that at high frequencies the loss is not proportional to the frequency.

Curves are sometimes drawn which are intended to show the ratio between the voltage which will break down a machine in one second and the voltage which will break it down in two seconds, and so on. It will be seen from the foregoing that such curves, even if plotted for different frequencies, different

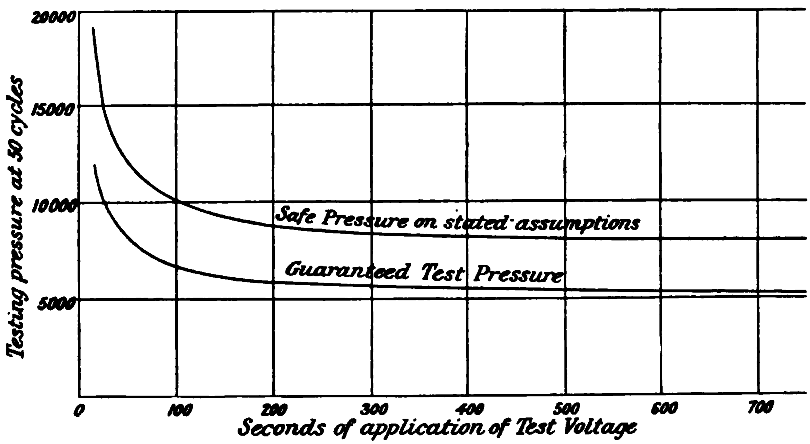


FIG. 213.—Relation between the time of application of pressure and the safe pressure to apply.

initial temperatures and different test pressures would still be very far from the truth unless they also took into account the cooling conditions of the insulation and its capacity for heat. To provide for all these matters when dealing with commercial machines would be impossible. Nevertheless, such a curve, with all its weakness from a theoretical point of view, is better than no curve at all. For it is clear that it is not fair to apply the same voltage for ten minutes that we would apply for ten seconds. We may, by prescribing the conditions not far removed from those obtaining in actual practice, plot a curve which at least contains some element of truth. If the testing voltage for a 3750-volt machine be taken at twice the working pressure, applied for sixty seconds, the frequency not above 50 cycles and the temperature of the test about 65° C., we may take the lower curve given in Fig. 213 as a safe curve from the manufacturer's point of view. This curve has been arrived at on the following assumptions. First, it may be assumed that the insulation should be designed to withstand continuously twice the normal pressure of the machine. Secondly, we may say that the insulation should be such that we will not

have in its weakest part a greater loss than 8 watts per cubic inch of insulation, even when four times normal pressure is applied to it. For this loss per cubic inch would heat up the insulation of the weak spot at the rate of one degree in four seconds. Taking, then, two watts per cubic inch (a loss easily dissipated) as the permissible loss in the weakest part of the insulation at twice the working pressure, the upper curve in Fig. 213 gives us the pressure tests which could be applied with equal safety for the number of seconds given by the abscissae. This curve agrees with the results found in practice so far as such irregular results can be made to agree among themselves. It would be idle to continue such a curve into the region between 0 and 10 seconds, because, if a machine breaks down in the first few seconds, it is clearly near the danger limit, and there may be so many reasons for this, such as condensed moisture, broken insulation or what not, that it is unprofitable to ask what would or would not have happened if the voltage had been applied for a shorter time.

The lower curve marked "Guaranteed test pressure" has been plotted simply by reducing the ordinates of the upper curve in the ratio of 1·5 to 1. In giving a guarantee we may take any points we like between the two curves according to the factor of safety that we may choose. The lower curve is particularly safe for the long duration tests. This is as it should be, because it is not wise to risk over-heating any weak points there may be in the insulation by a long application of excessive pressure.

These curves, of course, refer only to the risk of breakdown by over-heating. Sometimes the breakdown occurs through the air over the surface of the insulation. A breakdown of this kind is commonly preceded by a brush discharge, and the time of application of the voltage does affect the result, but in a way far too complex to be expressed on any curve.

The dielectric strength of the insulation on a machine depends very largely on its dryness; the presence of moisture increases the losses and the consequent heating. This matter is dealt with more fully under the next heading.

The test pressure which may be safely applied to a machine is dependent on the temperature of the insulation. If the insulation is thoroughly dry and at the same time cold (say 20° C.), it will withstand a 10-second voltage test about 50 % higher than if heated up to 70° C. This we can gather from Fig. 206. Here again our figures can only be rough, and do not at all take into account breakdowns arising from sparking over surfaces. When the insulation is warm and dry, there is less tendency for a flash over to occur.

PRESSURE TESTS.

The pressure test which should be applied to the completed machine to ascertain whether the insulation is strong enough, is a matter upon which a great deal has been written. The consensus of opinion appears to be that a fairly high-voltage test for a short space of time, say one minute, is more satisfactory than a lower voltage applied for a long time, say one hour, both from the manufacturer's and the purchaser's point of view. A high voltage will pick out and break down places

where the insulation is cracked much better than a lower voltage, however long it is applied, while the long application of the voltage to a machine which is slightly damp may spoil insulation which might otherwise get into perfect condition after a few weeks of service.

The British Electrical and Allied Manufacturers' Association have provisionally adopted the following tests applied for one minute between the windings and frame when the apparatus is at normal working temperature. The test should be made with a pressure of approximately sine wave-form, preferably at the rated frequency of the apparatus, but in general any frequency between 25 and 100 is satisfactory.

Rated terminal pressure of circuit.	Test pressure.
Not above 333 volts	1000 volts.
Above 333 but not above 1500 volts	Three times rated voltage with a minimum of 1500 volts.
„ 1500 „ „ 2250 „	4500 volts.
„ 2250	Twice rated voltage.

According to the German Standard Rules also, the test voltage should be applied for one minute. The voltage to be applied depends upon the rated voltage of the

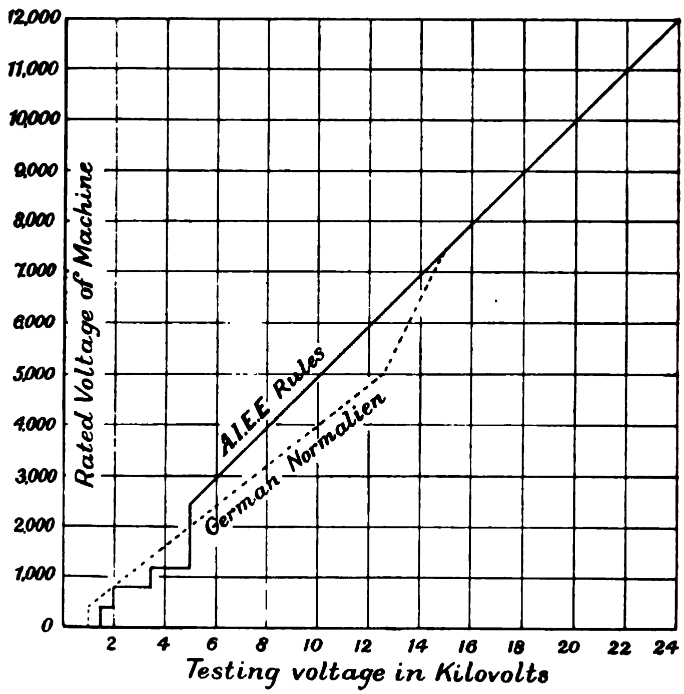


FIG. 214.—Curves giving the standard testing voltages for machines of various rated voltages in Germany and in America.

machine as indicated by the dotted line in Fig. 214. For machines designed for a pressure over 7500, the testing voltage is just twice the rated voltage.

The rules of the American Institution of Electrical Engineers are a little different for machines of low voltage. The full line in Fig. 214 gives the testing voltage for machines of 10 K.W. and over.

3. and 4. **Specific resistance and property of being unaffected by moisture.** The specific resistance of all the materials used in the insulation of dynamo-electric machines is, when dry, quite high enough for all practical purposes. When trouble arises from leakage, it is invariably due to the presence of moisture. The property of being unaffected by moisture is, therefore, one of the most valuable that an insulator can have. Mica is remarkable in this respect. All the papers, even when impregnated with varnish, gum or paraffin wax, will absorb moisture if left for a long time in a damp atmosphere. When damp there is no one value for the insulation resistance, as this is a function of the applied voltage. Evershed * has shown that for cellulose insulations the resistance at a pressure of 50 volts is about 3 times the resistance at a pressure of 500 volts. So long as a machine is in service and has its temperature kept above that of the surrounding air, its paper keeps dry; indeed, the tendency is for it to become too dry and brittle. The impregnation with varnish and gums and the exterior coat of varnish are very useful in keeping out the moisture during the short intervals when the machine is not running. If a machine has been out in a cold atmosphere and is reduced to a low temperature, and is then brought into an atmosphere in which the dew point is higher than the temperature of the machine, the moisture will collect in drops all over the surface and will penetrate to every place that is accessible to the air. When once moisture has got into a well-varnished armature, it is a rather difficult matter to get it out. Heating the armature at first merely has the effect of evaporating the moisture in one part of a coil and driving it into another part. Sometimes the moisture which is in the pores of the cotton fabric is driven to the surface, where it is more effective in reducing the insulation resistance. This is seen from the way that the insulation resistance * of a damp machine falls when it is first warmed up. The following observations were made on the insulation resistance, measured at 500 volts, of an armature of a 10 K.W. generator which had been stored for three years. Full-load current was passed through the coils to warm them up, and readings were taken of the insulation resistance at frequent intervals. The insulation resistance, which started at 0·5 megohm, in the course of one hour fell to $\frac{1}{100}$ of a megohm. In about $3\frac{1}{2}$ hours time it began to rise, and as the moisture was more completely dried out, the insulation rose to 60 megohms. Even when the resistance has been very much increased by passing a current through a machine, it must not be supposed that the drying-out process is complete. It sometimes happens that some parts of the insulation form a thin layer of dry cellulose, which has such a high resistance as to make it appear that the whole insulation is good; and it will be found that, if the current is taken off and the moisture in other parts of the machine

* See important paper by S. Evershed, "The Characteristics of Insulation Resistance," *Jour. Inst. Elec. Engrs.*, vol. 52, p. 51. Also "Electrical Conductivity of Press-spahn and Pilit," Tedeschi, *Archiv f. Elektrot.*, 1, No. 11, 497, 1913; "Hygroscopic Susceptibility of Fibrous Insulating Materials," W. Digby, *Inst. Civ. Eng. Proc.*, 183, p. 285, 1910-11.

allowed to redistribute itself, the effect of the heating-up process will again bring down the insulation resistance. If a machine is fairly dry, its insulation resistance, when cold, will always be much higher than when hot. This effect is most commonly due to the way in which the residual moisture distributes itself in a hot machine, though, apart from this, the insulating resistances of insulating materials are lower at high temperatures than at low temperatures.

5. **Capability of withstanding high temperatures.** The only materials used in the insulation of armature coils which will stand really high temperatures are mica and asbestos, and as these materials are usually employed in conjunction with other materials of a more perishable nature, the temperature * which the machine will withstand continuously is somewhat below 100° C. If it were commercially possible to insulate a coil both in the slots and on the ends with mica, and retain the mica in position with an imperishable insulating cement, it would, no doubt, be possible to run such an armature at 200° C. or more. Where asbestos is employed, it is usually in positions which do not require very high insulation. Asbestos, when not well dried out and saturated with some varnish, has very poor insulating properties, and the varnishes will all perish if maintained at temperatures much above 100° C. The effect of the temperature upon the cellulose insulation is very well shown in a paper of experiments made by Mr. Rayner.†

The materials were tested

(*a*) Unheated.		
(*b*) After being heated to		75° C.–100° C.
(*c*)	,, ,,	100° C.–125° C.
(*d*)	,, ,,	125° C.–150° C.

The list of materials tested covers the whole range of insulating materials commonly used in electric machinery. In general, it is found that materials such as press-spahn, manilla paper, oiled linen, and varnished tape, which were flexible at ordinary temperatures, had their flexibility somewhat reduced by being subjected to a temperature between 75° C. and 100° C. for six weeks or three months, and became exceedingly brittle when subjected to a temperature between 100° C. and 125° C. for the same period. The brittleness was tested by bending the material round pins of various diameters. A piece of press-spahn treated with shellaced varnish 0·34 mm. thick under treatment

(*b*) broke on being bent round a cylinder		$\frac{5}{8}''$	in diameter.
(*c*)	,, ,, ,,	$1''$	,,
(*d*)	,, ,, ,,	$1\frac{3}{4}''$	,,

It appears from these results, and from the general experience on electrical machines, that where the temperature is raised a little over 100° C., all the cellulose materials lose their tough nature, particularly if previously treated with varnish. We may, therefore, say that 100° C. is the maximum at which

* See page 256 as to permissible temperatures.

† "Temperature Experiments at National Physical Laboratory," *Jour. Inst. Elec. Engrs.*, vol. 34, page 617.

ordinary insulating materials would withstand continuously, and a safe maximum temperature may be taken as 90° C. for unvarnished papers and 80° C. for varnished papers. Plain untreated cotton covering will withstand temperatures below 100° C. continuously. Where the cotton is saturated with enamel, so as to make the coil into a solid block, it appears that even somewhat higher temperatures do not disintegrate it, although the cold insulation becomes extremely brittle.

6. **Heat conductivity.** All the heat which is generated in the conductors must pass out by conduction through the insulation. This can only occur by reason of a higher temperature existing in the copper than in the material, be it air or iron, which surrounds the insulation. The difference in temperature between the copper and the outside surroundings for a given amount of heat passing per square inch will depend upon the thickness of insulation and its heat-conducting properties. In column 5 of the table on page 177 are given the conductivity of the various insulating materials, expressed in the number of calories which will pass across one cm. cube of the material in one second for one degree difference of temperature between opposite faces of the cube. The figures given are necessarily only approximate, because so much depends upon the closeness of fibre. Paper which is very highly compressed has much higher heat conductivity than a paper of loose texture. The heat conductivity also depends greatly on the temperature. With cellulose insulating materials the conductivity at 80° C. is five times as great as at 20° C.

The passage of heat through insulating materials is more fully considered in the chapter on Heat Paths, page 221.

7. **Property of resisting oxidation and changes after a long period of service.** This is one of the most desirable properties for insulation to possess, and it is, unfortunately, not possessed by any materials except those of a stony nature, such as mica. Deterioration in papers, cotton and varnish, even when not excessively over-heated, may be due either to (1) desiccation; (2) oxidation; (3) injury from nitric acid, formed by brush discharge.

(1) If the cellulose insulators are deprived of moisture, they become exceedingly brittle, as shown in the experiments referred to on page 190.

(2) Many of the varnishes, such as Sterling varnish, linseed oil, etc., dry by a process of oxidization. This oxidization, which will go on slowly at normal temperatures, occurs much more quickly at temperatures over 90° C. When any material is treated with Sterling varnish, it is usually dried in an oven until it is set, but it should be left in a fairly flexible condition. The process of oxidization will, however, continue even at ordinary temperatures, and more rapidly at the temperature of a running machine, so that in time the flexibility can be no longer relied on. Where, however, a great number of layers are superimposed, each layer having been painted with varnish which is dried in position, the outer layers, to a great extent, keep away the air from the inner layers, and these may maintain their green condition for a number of years.

Where a coil is repeatedly heated and cooled, a "breathing" action goes on from all its pores; any air lodging in the interstices of the insulation breathes out when the coil is warmed, and breathes in when the coil is cooled. This process supplies fresh oxygen for the oxidization of the material. It is practically

impossible to prevent this action altogether. The only safeguard is to use in the insulation a large percentage of material which is not affected by it.

Formation of nitric acid. Wherever an electric discharge occurs through air, nitric oxide is formed, which will readily combine with any moisture present, to form nitric acid. This will then attack the copper, forming nitrate of copper; and if the action goes on to any marked extent, the insulation between the turns of the coil will break down.

The conditions which bring about a brush discharge from the conductor in the coils are as follows:

The brush discharge occurs by reason of the voltage which exists between the conductors in the slots and the iron of the frame. An air space adjacent to the conductor and lying between it and the iron, as shown in Fig. 164, may be subjected to so great an electric stress that it breaks down. This excessive stress in the air may be caused either by

(1) An insufficient thickness of the insulating wall.

(2) The nature of the wall being such as to allow a capacity current, or a conduction current, to flow through part of it and throw an excessive stress on the remaining insulation, or

(3) The shape of the conductor may be such that the lines of electric stress radiate from a comparatively small centre, as would occur in the corner conductors of Fig. 164, and bring about a high potential gradient next to the inner conductor.

The condition under which nitric acid is formed in the interstices of coils has been very fully treated in a paper * by Messrs. A. P. M. Fleming and R. Johnson.

They show that:

1. This action is rare in machines having a lower voltage than 3500 to ground.

2. The action only occurs where air pockets are present, and then only when the voltage across them is high enough to produce a discharge.

3. The gases produced by the discharge in one part may be carried to other parts of the coil.

4. The action of the products of the discharge (whether these be ozone or oxides of nitrogen) on the insulation is commonly one of oxidation, and the effects produced on different materials are:

> Untreated cellulose materials have their fibrous structure destroyed.
>
> The oils and gums used in varnishes are subjected to super-oxidation, and yield organic acids. Linseed oil is readily affected.
>
> Certain asphaltum compounds are very little affected, and paraffin wax is quite unaffected.
>
> Mica is unaffected, but the cements used in building it up are attacked.

5. The deterioration of the insulation may occur when no nitric acid is detected.

* *Journal Inst. Elec. Engineers*, January, 1911.

6. The disintegration of varnished materials is accelerated by the release of organic acids.

7. Though chemical action may bring about a short circuit between turns, it does not commonly cause a breakdown of the slot insulation between windings and ground.

8. If no breathing occurs, the chemical action will cease.

From a consideration of a number of high-voltage machines in which the chemical action has been observed, and other cases in which no action has occurred, Messrs. Fleming and Johnson come to the conclusion that, where the thickness of the insulating wall between the coil and frame is as much as one mil for every 35 volts, the danger from chemical action is very small, even though there may be air spaces in the coil and no special precautions taken. They suggest, therefore, that this thickness of insulation should be adopted wherever possible. In all cases it is the voltage to earth which must be considered. On an 11,000-volt three-phase star-connected machine, with the star point earthed, the voltage to earth cannot rise, under ordinary conditions, higher than 6350 volts; a thickness of insulation wall of ·18″ would, therefore, be sufficient on the above consideration.

Where it is impossible to provide insulation of this thickness, breakdowns from chemical action may still be avoided by adopting special precautions such as the following:

(*a*) The winding should be impregnated.

(*b*) The conductors, if possible, should be of rectangular section, so as to leave little air space between themselves, and should be arranged as shown in Fig. 160 (see page 138) rather than as shown in Fig. 162, so that there is the lowest possible voltage between turns.

To further safeguard against short-circuit, the conductors should have their insulating coverings reinforced on the slot portion of the coil by strips of mica or other insulation not affected by chemical action. Fig. 165 shows a good method of carrying out the arrangement of conductors and slot insulation on an 11,000-volt three-phase star-connected machine.

When the conductors in a coil are so small and numerous that separators cannot be used between them, they should be insulated to ground so heavily as to bring the stresses within the safe limit of 35 volts per mil. When such risky coils are used in high-voltage machines, surges or other causes of concentration of potential between turns near the terminals of the machine are very likely to produce short circuits. Wherever possible, coils of this nature should be avoided by winding the machine for a low voltage and using step-up transformers.

EXPERIENCE FROM BREAKDOWNS.

The successful designing of insulation must be based on the experience of previous failures. Certain materials assembled in a certain way have proved to be unreliable, while other materials or other methods of assembling have been proved to be good and safe. It is well therefore to look into a few of the most common causes of breakdown. These we will consider under the following headings.

Mechanical injury. Accidents sometimes happen to the insulation during the process of manufacture. A common accident is the cutting through of the insulation between two copper conductors by the application of excessive pressure between two hard surfaces. All parts of insulated conductors which may in the course of manufacture or during running in service be subjected to great pressure should be specially protected. Thus, in the making of a coil of insulated wire, it is good practice to give protection to all parts of the wire that may have to bear more severe treatment than the rest. The first few turns of the first and last layers should be taped or protected with stout insulation in addition to the cotton covering. At all points where the wire crosses over to the next layer it should be protected. Thick wires, and especially square wires, require protection at such points with very tough insulation such as press-spahn ·01″ thick. It is a good plan to put a rope or a cotton cord of a quarter of an inch diameter on the inside corners of wire coils. This cord replaces the first or the last turn of the layer of thick wires, as shown in Fig. 334, or several turns in the case of small wires, and gives mechanical protection to the coils, increasing at the same time the distance between the copper wire and any metal that may come near the corner of the coil after it is in place.

There are often parts of armature coils that require special protection either by extra taping or by the insertion of tough pieces of insulation. However small the risk of abrasion may be at any such point, it is generally worth while to insert additional insulation if there is room for it, and the cost of insertion is small. Wherever wires are not tightly held together, and the conditions are such that a certain amount of relative motion (however small) can occur between them, we must look upon the place as one of danger, and eliminate it altogether or take special precautions in the mechanical protection of the insulation.

The end of the straight parts of an armature coil just at the point where it leaves the slot is a rather vulnerable part, not only on account of the forces which are sometimes exerted on it in getting the coil into the slot, but also on account of the movement of the part which sometimes occurs when running. For this reason it is good practice to carry an extra layer of insulation between the conductors at the point even though the room taken up is considerably increased thereby. Stockings of cotton braiding which can be slipped over the conductor, and then drawn out so as to make a tight fit, are very convenient for giving protection at such places.

Great care must be taken in the selection of materials for the insulation of conductors on high-speed machines which are subjected to great centrifugal forces. If cotton fabrics and varnish are used to make an enclosed insulation, this should be supplemented with tough fuller board to withstand the pressure, and afford sufficient insulation if the other covering should by any chance be cut through. Mica is one of the safest materials to insert in cases of very great pressure evenly distributed. It is not, however, suitable if any sliding motion can occur between the surfaces which are pressed together. Fig. 212 shows a good way of insulating armature conductors which are subjected to great centrifugal forces.

The following are the steady pressures per square inch which different insulating materials will withstand when placed between two flat copper surfaces.

One column gives the safe pressure and the other the pressure at which mechanical breakdown occurs.

Material.	Thickness.	Safe pressure. Lbs. per sq. in.	Breakdown pressure. Lbs. per sq. in.
Calico - - -	·005	3,000	15,500
,, - - -	·010	4,000	16,000
Empire cloth -	·007	2,000	12,000
Linen canvas -	·010	4,000	18,000
Fibre - - -	·125	6,000	25,000
Fuller board -	·007	6,000	25,000
,, -	·032	6,000	25,000
Mica (pure white)	·010	10,000	40,000
Micanite - -	·010	8,000	38,000

Mica under test conditions appears to withstand compressive stresses as well as copper itself. The breaking up of the mica is due to the flowing of the copper in the direction at right angles to the direction of the applied force. This flowing of the copper tears the mica up into small pieces. The danger in loading mica is that it may be subjected to bending stresses due to inequalities in the surface upon which it is bedded. For this reason the compressive stress is sometimes kept below 1000 lbs. per sq. in. Sheets of mica between mild steel blocks withstand 90,000 lbs. per square inch, without showing signs of distress.

In designing an insulation to withstand mechanical forces, it should be remembered that all the cellulose insulations (paper, cotton and linen fabrics and the like) become very brittle after being in service for a long time at a high temperature. The cotton covering of field coils sometimes becomes nothing more than a dry insulating powder adhering more or less firmly to the wire.

Resistance to shearing stress. The only case of note in which insulating material is subjected to shearing stress is the case of wedges in the tops of slots. In direct current turbo-generators, the conductors are commonly retained in the slots by fibre, fish paper or wooden wedges, and in some cases the shearing stresses as well as the bending stresses in the wedges is quite considerable. If wood (hornbeam or beech) is used, the long way of the grain should stretch across the slot, so that the tendency is to break the wedge across the grain. The material should be treated with a varnish like Sterling varnish, and dried at a temperature not exceeding 90° C. The ultimate shearing stress of fibre or fish paper may be taken at 8000 lbs. per square inch, of hornbeam 6000 and of beech 3000 lbs. per sq. in. A factor of safety of 10 should be employed on account of the uncertainty of the fit of the wedge in the slot.

Over-heating. A not uncommon cause of breakdown is over-heating, which may either destroy the insulation by burning or make it so brittle that a mechanical breakdown occurs. Such heating is generally due to a cause over which the insulation designer has no control. But there may be cases where the trouble is mainly caused by the way in which the insulation is carried out. Cases have been known in which the current density in the copper has been quite low, and in which the main part of the armature coils have been comparatively cool during the running of the machine, and yet certain parts of the armature coils have

become hot enough to char the covering, owing to the fact that the insulation has been put on in numerous layers which enclosed layers of air, thus forming a very perfect non-conductor. If half the number of layers of insulation had been used, and care taken to exclude most of the air by sticking the successive layers together with varnish, the space thus saved would have allowed the air to circulate between the coils and a burn-out would have been avoided.

Over-heating and charring of insulations has been sometimes caused by the so-called drying-out process to which a machine is subjected after it has been standing idle in a damp situation. Current from an independent source is passed through the windings for the purpose of warming them up and drying them. Thermometers are placed in certain parts of the coils to see that they are not too hot, but as the machinery is stationary, and there is no proper circulation of air, certain other parts of the coils, whose temperature is not indicated, have been raised to excessive temperatures.

It is rare that over-heating occurs from excessive stresses upon insulation, but where an excessive voltage test is applied to a machine for a long time, it is possible for the insulation to be over-heated locally, as described on p. 179.

Trouble from oil. Sometimes oil from the bearings will get upon the armature coils and weaken the insulation, so that it breaks down. On railway motors and on machines where there may be difficulty from oil-throwing, the insulation should be specially enclosed in an oil-proof sheath.

Trouble from voltage breakdown. The cases in which the insulation of an armature breaks down on the application of high voltage may be divided into two broad classes—(1) where the puncture occurs through the insulation directly from conductor to iron, and (2) where the breakdown occurs across a surface. The first class of breakdown is nearly always due to some mechanical defect. For instance, a cell of mica and paper may have been crumpled so that the mica is crushed. Trouble of this kind can only be avoided by careful supervision of the methods of wrapping and the provision of a sufficient factor of safety in the number of wraps used.

A common experience in applying a voltage test to a high-voltage machine is for a breakdown to occur over a wide surface of insulation between the iron and the ends of the coils. The action is a very complex one, and in many cases is difficult to account for, a voltage of 16,000 volts travelling over a surface of perhaps $2\frac{1}{2}''$. The outer surface of the insulation forms a condenser with the copper conductor, and this condenser is charged and discharged by sparks which creep, at first only a short distance, along the surface. The air, having become ionized by these sparks, becomes a conductor, so that the sparks are able to creep further and further along the surface, until at last the spark jumps direct from coil to iron. To avoid this action, it is necessary to provide an amount of creeping surface, specified on page 172, and at the same time to seal, as far as possible, the ends of the insulating tubes.

Collection of dirt. One of the commonest causes of breakdowns is the accumulation of dirt. Bar-wound machines of low voltage, with certain parts of the insulation left bare, may stand up well on their original test, but when running in service, dirt of a more or less conducting character may cause a short-circuit.

For this reason, it is best, wherever possible, to use a completely enclosed insulation, which will be sound, however dirty it may be. In cases where the conductors must be exposed, as, for instance, on commutators, not only must long creeping distances be allowed from copper to frame, but this surface must be of such a character that it cannot easily become dirty. An inside surface upon which dirt will collect and be held by centrifugal forces will not do. The surface, if possible, should be designed so that the dirt is thrown off it, and, if possible, all such surfaces should be accessible, so that they can be cleaned.

Breakdown between turns. Breakdown between turns due to mechanical injury has been considered in a previous paragraph. Sometimes the breakdown occurs through the voltage between turns being very much in excess of the normal voltage. This may occur in those coils of high-voltage machines which are nearest the terminals. When, for instance, an induction motor is suddenly switched on to the busbars, an electric wave, originating at the switch, passes along each conductor with enormous rapidity. The steepness of the wave front will depend upon the capacity of the conductors connected to the switch, the self-induction of the cables and the manner of making the contact. It is sufficient to say that, in some cases, the steepness is so great that, as the wave passes into the coil, it creates an excessive difference of pressure between the ends of each turn, a pressure of perhaps thousands of volts. If the insulation between turns has only been designed to stand a low pressure, it may break down under these conditions. It is a good plan, on high-voltage machines which are to withstand sudden switching-on, to design the insulation between each individual turn of the first few coils near the terminals so that they will stand instantaneously the full voltage of the machine. For this purpose it may be necessary to cut down the number of turns in these coils to get sufficient room. The voltage between turns on field coils under normal running conditions is usually very small, but in designing the insulation it must be remembered that, if the field current is suddenly broken or if an explosive arc occurs in the armature current, a very much higher voltage may be thrown on each turn. It is therefore well, on field coils, to be not too sparing with the insulation between successive layers, and the insulation to ground must be capable of standing 3000 or 4000 volts on field coils excited at 125 volts and of withstanding proportionally higher voltages in cases where the exciting voltage is higher.

METHOD OF INSULATING COILS.

This subject is such a very large one that it requires a book to itself, so nothing more will be attempted here than a statement of the salient points. Messrs. Fleming and Johnson in their book* have treated of the matter very fully both theoretically and practically, and given numerous diagrams of shop methods. For further information the reader is referred to that book and to other treatises† which specialize upon the subject.

* *Insulation and Design of Electrical Windings.* (Longmans.)

† Turner and Hobart, *Insulation of Electrical Machines.* (Whittaker.)

The insulation of the different parts of a machine may be considered under the following headings:

(1) The insulation and assembly of the separate conductors.
(2) The insulation of the slot.
(3) The insulation of the end windings.
(4) The insulation and support of the terminals.

(1) **The insulation and assembly of the separate conductors.** The cotton covering which has been for so long used to insulate the separate turns of a coil from one another still seems the best and cheapest material for wires not very small (say greater than 0·05″ in diameter). For very small wires the cotton covering takes up a great deal of room. Silk covering is rather expensive, but for small wires it pays for itself by the economy that can be effected in the size of the machine. Enamelled wire has largely come into use for small sizes, and as the price of this wire will probably be much lower in the future, one may expect it to be very commonly used. The thickness of enamel only adds ·001″ to the diameter of a ·01″ wire, as against ·0015″ for single silk, ·0025″ for double silk, ·005″ for single cotton, and ·009″ for double cotton. Enamelled wire is generally wound with very thin paper (·003″) between layers. This paper takes up a good deal of room, so that the saving in space with enamelled wire is not as great as it otherwise would be. The voltages (50 cycle alternating) which two round wires ·05 in diameter at a temperature of 20° C. can withstand for one minute when pressed together with a force of 10 lbs. per linear inch are given below:

	Total thickness of insulation between two wires.	Puncturing voltage.
Untreated cotton covering	0·012″ thick	1250
Paraffined ,,	·012″ ,,	2000
Impregnated (petroleum residue)	·012″ ,,	2000
Varnish (Sterling)	·012″ ,,	2000
,, (Shellac)	·012″ ,,	1500
Untreated silk covering	·0035″ ,,	500
Paraffined ,,	·0035″ ,,	1500
Varnish (Sterling)	·0035″ ,,	1500
,, (Shellac)	·0035″ ,,	900
Enamelled wire	·001″ ,,	300
Enamelled wire and one piece of ·003 paper impregnated with gum	·005″ ,,	1300
Aluminium with normal oxide coating	less than ·001″ ,,	200

The voltage between adjacent wires in field coils and small armature coils is usually very small, so that the most important quality of the insulation is that it shall resist abrasion during the process of manufacture, and shall not deteriorate on the running machine. The heat conductivity of the assembled wires is also of great importance, especially in the case of thick coils with a great number of layers. This matter is dealt with fully on p. 221.

The space occupied by a number of round insulated wires depends very largely on the bedding of the layers. When the wires are small, and particularly if the coil is of rectangular shape, one cannot be sure that the turns of one layer will be

in the hollows left by the last. Where the wires are of fair size (0·1″ diameter), and even for smaller wires if the coil is of cylindrical shape, one can, by the exercise of a reasonable amount of skill, rely on the theoretical amount of bedding being obtained within a few per cent. In Fig. 166 we give the space factor (that is the ratio of cross-section of copper to cross-section of winding space) to be found in coils with ordinary skill with different sizes of wire. With a coil made to fit a rectangular pole, it is much more difficult to make the wires bed into the grooves. Some makers use extra insulation between layers at the corners, and this prevents bedding altogether. Such coils often have a space factor as low as ·5 when wound with D.D.C. wire ·05″.

It should be noticed that when one layer is wound over another, there must be a point somewhere in each turn where the wire crosses the wire lying under it. If this crossing over is done regularly at one side of a coil only, and occupies only a short length of the wire, the bedding may be good for all the rest of the coil, but if it is done irregularly, it leads to bad bedding throughout the coil.

Armature coils. The insulation between turns of armature coils must be carried out with the very greatest care, as the short circuiting of a single turn is very disastrous, whereas the short circuiting of a turn on a field coil is of comparatively small importance. Double cotton-covered wire is commonly used for armature coils where the size of wire is not greater than No. 12 S.W.G., and where the voltage between turns is not greater than 25. Double cotton covering is preferred for armature coils because it is not so liable as single cotton to open out and show bare copper at places where the wire is bent around a corner of small radius. At parts of a coil winding where abrasion may possibly occur during the winding or during the operation of the machine, it is advisable to supplement the cotton covering by a layer of tape or tough paper. In the case of square or rectangular wire, a very tough paper or leatheroid should be used between turns at cross-overs and at all points where the corner of the rectangular wire may bear with undue stress upon the cotton covering. Cotton covering is much improved, both mechanically and electrically, by being treated with a tough varnish. If the wire is treated before winding, it should not be dried so hard as to cause the covering to crack when wound around corners. Armature coils and other windings of no great bulk are commonly dipped, after being thoroughly dried, in a copal varnish, and the outer layer of varnish is oxidized in an oven and forms a coating which is fairly efficient in keeping out moisture. When a coil is very bulky, it is impossible to oxidize the varnish on the inner part, so it is better practice to impregnate coils of considerable section in a vacuum oven, and force the impregnating compound into the interstices of the coil by the application of external pressure. For larger wires, the insulation should be supplemented with a wrapping of tape. Large round wires are now seldom employed in armature coils, wire of rectangular section or strap affording a much better space factor and giving better mechanical support. Double cotton covering is used on very small straps, but an insulation of mica and paper is to be preferred on the straight part of the coil. The common method of insulating the straps of a direct-current armature coil to stand up to 500 volts is to interleave one end of a sheet of mica paper

between the straps, and then to wrap the remainder of the sheet $2\frac{1}{2}$ or $3\frac{1}{2}$ times round the conductor as a whole. This, of course, can only be done on the straight part of the coil. In the diamond-shaped or involute ends of the coils, each strap must be separately taped or alternate straps may be taped with an overlapping layer, and the whole coil treated before the mica-paper insulation is applied. The tape on the ends must be continued for $\frac{5}{8}''$ into the straight cell, and for this reason the straight cell is made to project $\frac{3}{4}''$ beyond the slot. This portion of an armature coil, just at the ends of the slots, has given much trouble in the past, and requires very careful treatment. If two coils lie one above the other, it is desirable that the cells of both coils should not end at the same point. For this reason, one of the cells is made to project a full inch, as shown in Fig. 198.

FIG. 212.—Shows the method of taping the individual turns of a concentric winding.

In A.C. generators, the voltage between adjacent conductors is sometimes of the order of one or two hundred volts, and at instants of switching on may amount to several thousands of volts. In these cases it is well to have a really good mechanical separation between the conductors, such as a strip of mica or mica paper, and each conductor should be separately taped with overlapping layers. Fig. 212 shows the method of taping the individual turns of a concentric winding. After assembling, the insulation at each successive turn should be tested at 3000 volts, where the voltage per turn is not more than 75 or 100 volts, and 5000 volts between turns if the voltage is over 100 volts per turn. It was stated on p. 197 that it is well to insulate adjacent layers of the first and last coils in a high-voltage machine, so as to withstand for an instant the full pressure of the generator between turns.

(2) **The insulation of the coil from the slot.** The most reliable material for the insulation of the coil on the straight part is mica, because it resists damp and does not undergo any change with time. A cell or tube of mica built up of shellac is,

however, rather too brittle, and it is therefore better to use some paper in the composition of the cell. Some makers use mica and empire cloth. This gives a cell rather more flexible than the pure mica,* and if the portion of the coils which projects through the slot is accidentally bent through a small angle, the paper or empire cloth affords a cushion to the mica, and prevents it from cracking. The manufacture of these wrappings has undergone a long course of evolution, and a great deal of experience and skill is necessary to produce a really well-wrapped coil free from avoidable air spaces and yet of sufficient flexibility. The thickness of mica and paper wrapping to be employed may be taken from the data given by Messrs. Fleming & Johnson in their paper above referred to. In no case should the thickness be less than ·04″. Having provided sufficient mechanical strength, the provision of $\frac{1}{1000}$″ of insulation for every 35 volts above earth has been found to be sufficient in actual running machines. Where the space available on a machine does not permit of the full thickness according to this rule, then special precautions must be taken, as stated on p. 193, to prevent injury due to brush discharge.

There are two general methods of applying the external insulation to an armature coil. (1) The insulation may be wrapped around the conductors and completely finished before being put on the machine. (2) The slot in which the coil is to lie may be lined with an insulating tube or trough before the conductors are inserted.

The first method is to be preferred for high-voltage machines, because it enables the coil to be properly sealed and made into a complete whole, able to withstand a high-flash test and prevent the formation of nitric acid (see page 192). The second method must be used in those cases where individual conductors must be inserted in the slot one by one. The mush-wound coils illustrated on page 122, the field coils illustrated in Fig. 133, and all hand-wound coils are instances where the slot must be lined with insulation before the insertion of the conductors. In all such cases it is desirable, where the carcase is not too big, to place the whole machine when completely wound in a vacuum oven, and, after exhausting all moisture, to impregnate the windings under pressure. This has the effect of sealing the overlapping layers of insulation at the openings of the slots.

EXAMPLES OF CALCULATIONS OF ROOM TAKEN BY INSULATION OF ARMATURE COILS.

Mush-wound coils for voltages up to 600. The slot may be insulated with one piece of press-spahn 0·05 cm. thick, and one piece of varnished cloth, such as "Empire Cloth," of a thickness of 0·02 cm. After the wires are inserted, the cloth is folded down so as to overlap. A suitably shaped tool is then pushed between the press-spahn and the overhanging tops of the teeth to press the edges of the slot lining well down and enable a strip of leatheroid or press-spahn about 0·08 cm. thick, and almost as wide as the slot, to be inserted as a lid to the slot. The parts of the coils lying outside the slot may be taped with "Empire" tape, and then with ordinary cotton

* "Experiments with Paper-free Mica Tubes," K. Fischer, *Elektrotech. Zeitschr.* 31, p. 239, 1910; "Artificial Mica," *Electrochem. Ind.*, N.Y., 6, p. 257, 1908; "Use of Mica as an Insulator," F. Wiggins, *Elect. Rev.*, 71, 564, 1912.

tape. After the coils are connected, the whole is impregnated in a vacuum tank. Mush windings of this kind may be wound either with one coil per slot or with two coils per slot. In the latter case, the varnish cloth covering must be lapped over the lower coil and a spacer 0·05 cm. thick inserted before the cloth insulation of the upper coil is inserted. Special taping of the parts of the coils lying outside the slots is necessary to prevent coils of opposite polarity from coming in contact with one another.

To arrive at the amount of space required for the insulation of continuous-current armatures for voltages up to 600, we can make the calculation as follows: The paper will be about 0·013 cm. in thickness, and the mica may make up the total thickness to 0·025 cm. The curved part of the coils lying outside the slot may have alternate straps taped with 0·015 cm. tape, the whole coil being dipped and dried before the straight parts are insulated. Mica tape on the ends is sometimes used where there is some fear of the coils being subjected to a high temperature or exposed to moisture. The external insulation of such a coil may consist of 2½ turns of mica and paper or mica and cloth, each turn being about 0·025 cm. thick. The whole coil is then taped over with cotton tape, which is half lapped on the projecting ends and wound without overlapping on the slot portions. A slot lining of 0·02 cm. paper is generally used with coils of this kind, making the total thickness of external insulation about 0·1 cm. from copper to iron. The width of slot in cms. required is therefore

$$mt_s + 0{\cdot}025m + 0{\cdot}2 + f_s,$$

where m is the number of straps side by side, t_s the thickness of the straps, and f_s is the allowance made for roughness inside the slot, usually 0·05 cm. to 0·07 cm. Between the upper and lower coils lying in the same slot, it is well to place a piece of 0·050 press-spahn, so that the taped portions of the coil which are of opposite polarity may not be too near together. It is also well to place a liner at the base of the slot, which may be of 0·025 press-spahn.

Table X. Allowance of Room in Slot for the External Wrapping of Armature Coils of A.C. Generators and Motors.

Voltage of machine.	Length of iron up to 30 cms.		Length of iron up to 100 cms.		Length of iron above 100 cms.	
	In width.	In depth.	In width.	In depth.	In width.	In depth.
2,000	·26 cm.	·36 cm.	·35 cm.	·47 cm.	·45 cm.	·58 cm.
4,000	·32	·42	·42	·54	·52	·67
6,000	·4	·5	·47	·59	·58	·73
8,000	·45	·55	·53	·65	·62	·77
10,000	·5	·6	·58	·7	·68	·83
12,000	·6	·68	·65	·76	·72	·87
14,000	·65	·75	·75	·87	·85	1·0

The stator coils of A.C. generators and motors. Where the conductors are small, double-cotton covering is used. For this allow a thickness of ·015 cm., making the space occupied by each conductor ·03 cm. wider and deeper. Where two conductors are in parallel, as in Fig. 161, the space occupied by the double

cotton covering in width will be ·06 cm. Where the arrangement is as in Fig. 162, an allowance of ·08 cm. should be made for each strip of press-spahn. Where the conductors are taped individually, the thickness of tape is generally ·04 cm. radially, giving a total extra width of ·08 cm. In high-voltage machines a strip of mica will generally be placed under the tape about ·05 cm. thick. The room to allow for external insulation can be taken from Table X.

(3) **Insulation of the end windings.** The distance which a straight cell should project beyond the slot for different voltages is given in Table VIII. page 172.

For all voltages over 3500 it is very desirable to have the insulation completely sealed at the ends of the cells in order to avoid the creeping action described on p. 196. Where open cells are used, the coil can be impregnated as a whole, and a well-sealed insulation obtained.

Where end connectors are employed, these should be individually taped with overlapping layers of tape or Empire cloth, the whole being treated with varnish, re-taped, and treated several times in succession until the connector will withstand, when in position, the full voltage of the machine. In assembling, the connectors are separated by press-spahn at all parts where they come under clamps, and treated wooden blocks are used to preserve a good sparking distance to earth.

(4) **The insulation and support of the terminals.** The conductors which lead from the winding to the terminals of the machine are usually insulated with successive layers of treated tape of such thickness as to withstand the full test pressure of the machine.

In bringing out the terminals of a high-voltage machine, the greatest care must be exercised. Only materials which retain their mechanical qualities should be used. Rubber-covered cable is not recommended, as it may soften with the heat or become saturated with oil. The terminal conductors must be held firmly in position by strong clamps, so that the insulation of these conductors must be of a kind that can resist mechanical pressure for any length of time. Some makers attach the conductors to porcelain insulators. This if carried out on a very substantial manner is good, but even the strongest porcelain insulators are sometimes broken in shipment. Another plan is to wrap the terminal conductors with a great number of layers of varnished cloth, each layer being treated with Sterling varnish before the next is applied. In this way a very tough and strong insulation can be built up, which entirely closes the conductor in and which can be clamped between wooden cleats.

When large generators and motors are installed, it is seldom worth while to provide terminals to the conductors which can readily be connected and disconnected, because one connection is all that is generally necessary in the lifetime of the machine. It is sufficient in general to provide thimbles by which a permanently sweated connection can be made. The thimble should by preference secure the cable without relying on the solder for mechanical support.

CHAPTER IX.

VENTILATION.

THE high electrical **outputs** which, in modern machines, are obtained from comparatively small **amounts of material** are due mainly to the improvements that have been made in **methods of ventilation.** The subject is therefore one of the greatest importance from a commercial point of view. The tendency now is to design a machine as we would design a blower,* providing definite paths for the air as it comes in, an efficient means of blowing, and a definite path for the air to the point where it is expelled.

Before proceeding to consider the various systems of forced ventilation, we will take up a few important matters relating to **self-ventilating machines**, that is to say, machines through which the draught is produced by no other means than the rotation of the working parts.

The first point is that the **general shape of the frame** and of the rotating part should be such that the warm air is thrown far away from the machine. The tendency for hot air to rise is not always sufficient to take it away from the neighbourhood of the machine, and it sometimes happens that the same air is drawn into the machine again and again, thus causing a very much higher temperature than would be obtained if the main supply of air were at the temperature of the room. A common cause of this trouble is the shape of the end bells or overhanging frame, which gives to the expelled air a horizontal direction and carries it to the vicinity of the intake. In self-ventilating machines, we ought to see that the centrifugal blowing action of the rotating parts is allowed to give the air a **velocity which takes it well away from the intake**. Sometimes a continuous-current generator which will run fairly cool when running by itself will have an excessive temperature rise when direct coupled to a motor on account of the interference with its scheme of ventilation. A little forethought and suitable shaping of the parts will obviate difficulties of this kind. With motor-generators

* The following references will be useful to the reader: "Turbo-generators and High-speed Motors," Niethammer, *Zeitschr. Vereines Deutsch. Ing.*, 53, pp. 1009, 1313, 1406, 1909; "Cooling Ducts, Use of in Electrical Machines," T. Hoock, *Elek. u. Maschinenbau*, 28, p. 908, 1910; "Ventilation of High-speed Dynamos," K. Czeijà, *Elektrotech. Zeitschr.*, 33, pp. 313 and 343, 1912; "Ventilation of Turbo-alternators," E. Knowlton, *Electrician*, 70, p. 259, 1912; "Ventilation Arrangement for Large Generators," Weltzl, *Elekt. u. Maschinenbau*, 31, p. 10, 1913; "Air-filtration, Cooling and Ventilation of Electrical Machinery," Christie, *Electrician*, 71, p. 452, 1913.

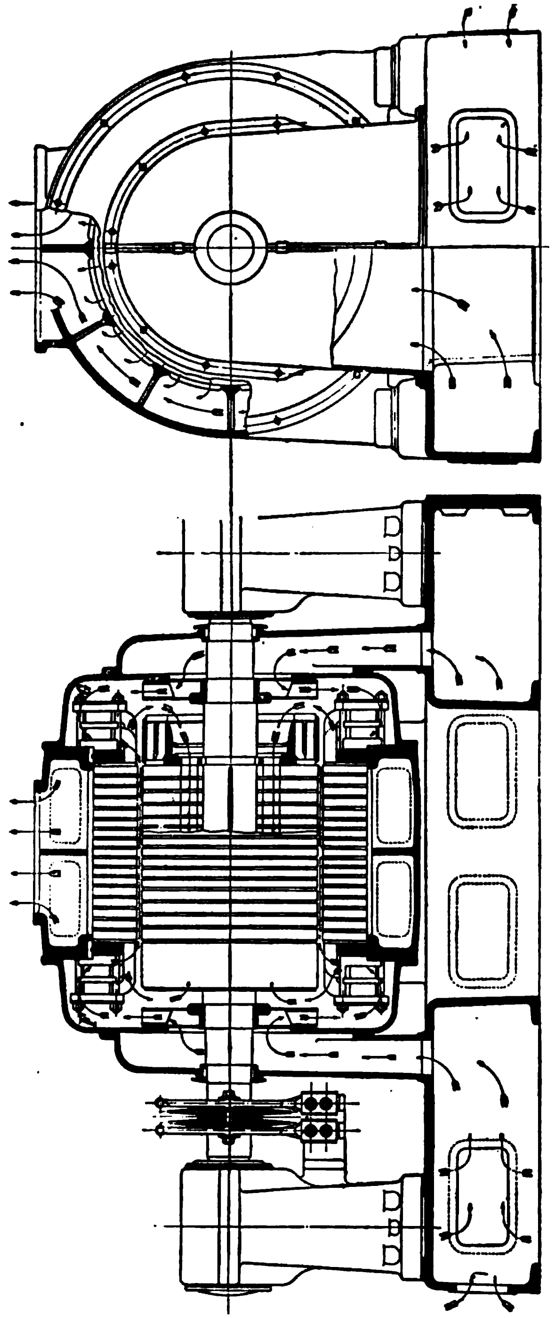


FIG. 215.—Scheme of ventilation of turbo-generator in which the cooling surface is provided mainly by radial ducts.

it is a good plan to arrange the fanning action so that the air is blown out radially between the two machines and drawn in at the ends of the set. Sometimes the flywheel adjacent to a generator direct coupled to an engine will prevent a proper supply of air to the electrical machine, while a very little **variation in the disposition of the parts** might make the flywheel improve the ventilation. Sometimes a machine, when running by itself, will be fairly cool, but when **adjacent machines** are running it gets hot from the air thrown off by them. It is therefore necessary, when checking the actual temperature rise with the rise expected from the calculation of the machine, to see that there are no abnormal external circumstances which make the temperatures either higher or lower than they would be on a fair test. For instance, if a dynamo intended to be direct coupled to an engine is on test driven by a belt, the **windage from the belt** will sometimes keep down the temperature rise by several degrees.

Amount of air required. Sufficient air must be provided to carry away the heat generated. A supply of 100 cub. ft. of air per minute for each kilowatt loss will in general be sufficient. If the conductivity for heat of all parts is sufficiently good and the air is so evenly distributed that none of it receives a temperature rise greater than 32° C., it may be that 60 cub. ft. of air per minute would be sufficient to keep the machine below 45° C. rise.

Having provided a **supply of cool air** to the machine as a whole, the next step in the ventilation problem is to see that the openings in the spider are sufficient to carry the air to the ventilating ducts. One of the reasons why machines of short axial length come out in practice to be more economical than would be expected from a calculation of the amount of material they contain is that the supply of air from the two ends, not only to the end windings, but to the ventilating ducts, is much better than on machines of greater axial length and smaller diameter.

The ventilating ducts themselves must not only be of sufficient cross-section to allow enough air to pass; they must also present sufficient cooling surface for the heat to pass from the iron or copper to the air. In the next chapter we give specific figures for the amount of air required and for the rate of passage of heat from the various surfaces. In this chapter we are only concerned with the general schemes of ventilation.

Schemes of ventilation. Fig. 215 illustrates a scheme of ventilation commonly met with in turbo-generators. Here centrifugal blowers are placed at each end of the rotor, and supply air to the completely enclosed ends of the machine. The air, after blowing over the armature coils, finds its way partly along the air-gap and partly through axial ducts in the rotor, from which it is thrown out by the radial ducts. The air then passes through the radial ducts in the stator iron to the annular space in the frame, and is finally expelled at the top of the machine.

When a machine is of great axial length, it is sometimes not possible to get enough air along the axial holes in the rotor, and for this reason other methods must be adopted. One method, illustrated in Fig. 216, still employs radial ducts, but the air is caused to flow inwards radially in some sectors of the machine, and outwards radially in others. The figure is self-explanatory. It will be seen that not only is there practically no limit to the amount of air which can be supplied

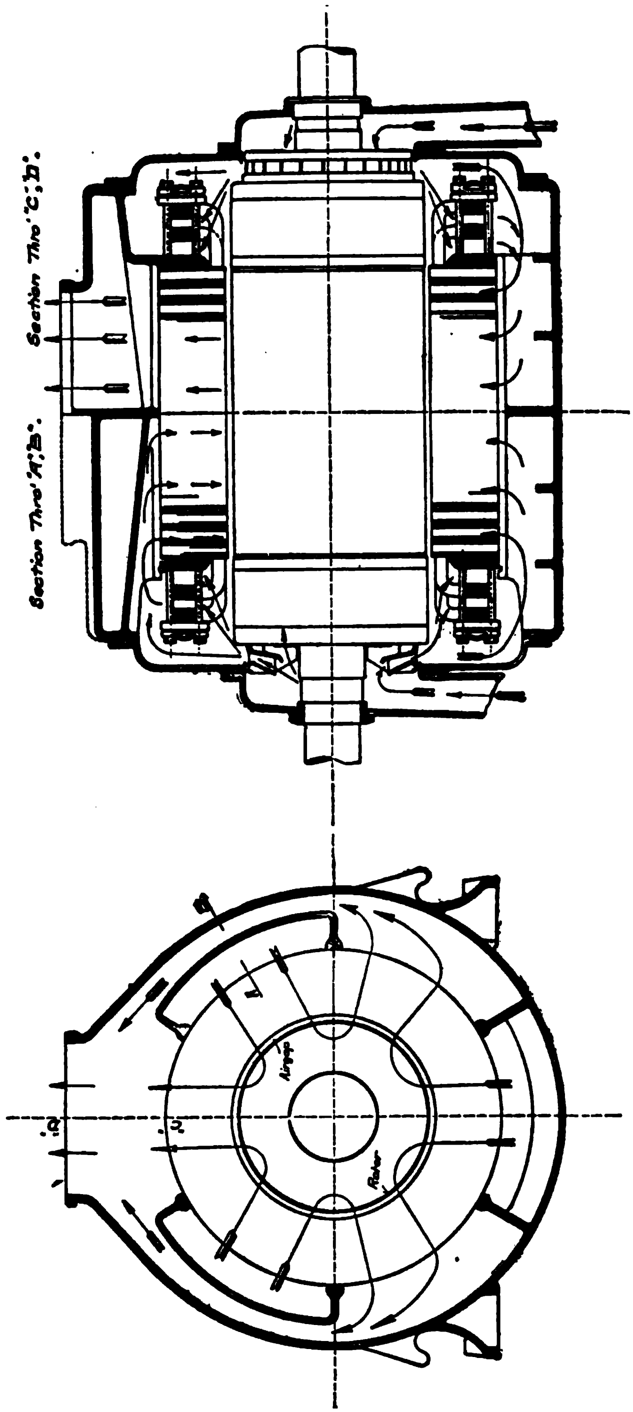


FIG. 216.—Scheme of ventilation by means of radial ducts into which the air is fed from various sectors of the frame and exhausted into other sectors. (Brown-Boveri et Cie.)

to the middle of the machine, but the air coming direct from the fan is cooler than air which has passed through the rotor.

Radial ducts and axial ducts. The cooling surface necessary for the passage of the heat from the iron to the air may be provided either by means of radial ventilating ducts, as illustrated in Figs. 215 and 367, or by means of axial ducts, as shown in Figs. 218 and 220.

Radial ducts are made by placing "ventilating plates" at frequent intervals between the ordinary punchings, and are convenient in design, in so far as the number of them can be easily altered to suit the circumstances of each case without any interference with standard punchings. In the rotating part they act as blowers, drawing their own air in machines that have no separate blower, and supplementing the special blower when one is provided. Fig. 429 shows the scheme of ventilation of a 75 K.W. D.C. generator fitted with a blower at the end opposite the commutator. In this machine the rear-end casting is formed so that it converts the rotational motion of the air into an outward blast whichever way round the machine is run, and thus the fan acts as a fairly efficient blower, causing the air to enter at the commutator end. Part of the air is drawn through the channels in the armature and part is drawn between the field coils. The space between the fan and the rear end of the armature is contracted and throttles the flow somewhat at this point, so that while a sufficient amount of air is drawn through the armature to feed the ventilating ducts, the blowing action of these ducts is not overpowered by the sucking of the fan. It will be seen from the calculation of this machine given on page 489 that the cooling coefficients of the field coils and armature coils are greatly increased by the use of the fan. A somewhat similar system of ventilation is illustrated in Fig. 217, but here the spider is completely closed at the slip-ring end. The air drawn in by the fan and ventilating ducts is driven through the stator to the opposite end of the machine.

In Fig. 218 is given the scheme of ventilation of a railway motor, in which the fan is placed at the commutator end, and, instead of radial ducts, we have axial holes running through the armature.

Axial ventilating ducts do away with the necessity for ventilating plates, and thus enable the straight part of the armature coils to be made somewhat shorter in those cases where sufficient iron can be obtained behind the slot without lengthening the frame to make up for the space occupied by the holes in the punchings. The conduction of the heat takes place very much more readily along the direction of lamination than across the laminations (see page 251), so that the heat travels through the iron to the surface of axial ducts more readily than it does to the surface of radial ducts. In practice, however, radial ducts are placed at fairly frequent intervals, so that the drop in temperature between the centre and edge of a packet is not of very great importance (see page 390 and Fig. 367). Axial ventilating holes should not be made too small, as the rough surface presented by the edges of the punchings renders them rather liable to be stopped up by dirt. A diameter of 30 millimetres or more is usual. The holes being fairly large in diameter, the ratio of the area of duct surface for a given length to area of cross-section is much smaller than for radial ducts. For this reason one

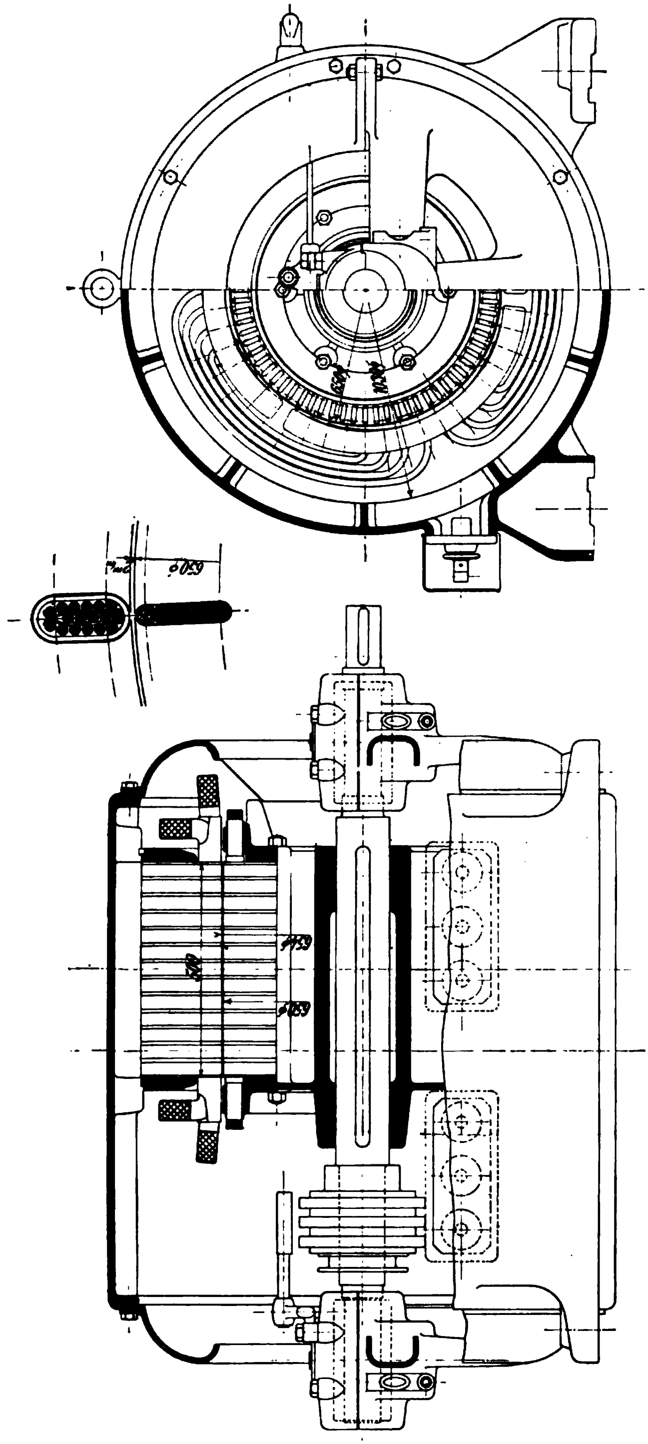


FIG. 217.—Three-phase motor by Brown, Boveri & Co., 600 H.P., 5000 volt, 1500 R.P.M., showing scheme of ventilation in which air is drawn in at one end and expelled at the other. Scale 1 : 18.

would expect air to travel for a greater distance along an axial duct before it attained its full temperature rise. The presence of the holes in the punchings interferes with the magnetic circuit, so that either a much greater depth of iron must be used or the total length of iron in the machine must be increased. When the depth of the punchings is increased there is no limit to the amount of air that can be supplied to the centre of a machine by means of axial ducts. This is of great importance in the design of very large turbo-generators.

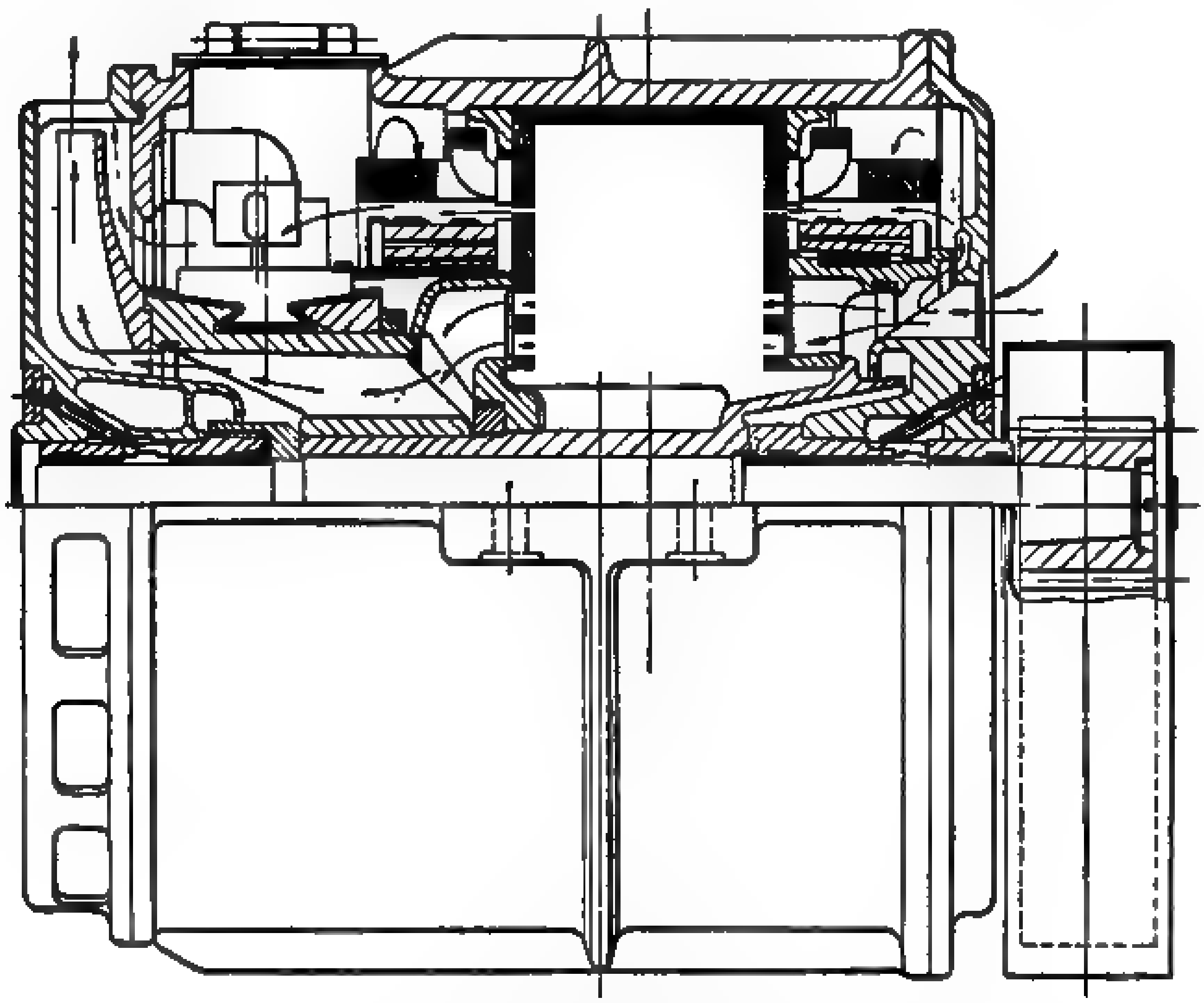


FIG. 218.—Scheme of ventilation of railway motor in which air is drawn through axial ducts by means of a fan outside the commutator

Fig. 219 shows a scheme for ventilating a turbo-generator in which air is blown from both ends of the machine through axial ducts to radial passages near the centre of the armature iron, whence it escapes into the annular space around the frame. This method is suitable for machines that have great axial length. Where the axial length is not too great it is sufficient to blow the air from one end of the machine only.

The method of axial ventilation, in which the air is passed from one end of the machine to the other, is well illustrated in Fig. 220. The rotor is of the type shown in Fig. 361, and has no radial ducts. Below the space in each slot provided for the winding there is a channel for carrying air. The air thus passes close to the place where the heat is produced, and by cooling the root of the teeth enables the heat to pass readily through the insulation of the coils in the slots. The blower at one end of the rotor (the left-hand side in Fig. 361 and the right-hand side in Fig. 220) consists of two parts. The inner part throws out

the air from the closed end-bell and causes it to be drawn in from the opposite end of the rotor along the ducts immediately below the conductors. The other part of the blower supplies air for cooling the armature conductors at that end of the machine. On the other end of the rotor (the right-hand side in Fig. 361 and the left-hand side in Fig. 220) a specially wide blower is provided, which supplies the air to cool the armature coils at that end and also to cool the armature iron. The air, after being forced into the enclosed end-bell of the stator (where it is received by suitably shaped surfaces which convert its tangential velocity into pressure), passes along numerous axial holes in the stator iron to the other end. It then passes through holes in the stator frame into the annular space behind the iron, from whence it is conducted to a flume at the base of the machine. The passing of the air through the machine from one end to the other

FIG. 219.—Scheme of ventilation of turbo-generator by means of holes in the stampings parallel to the axis of rotation; air passing from both ends of machine (Messrs. Siemens).

will of course cause one end to be hotter than the other; but there is no serious disadvantage in this, provided both ends are cool enough. The fact that only warm air is provided for cooling the inside surfaces of the rotor conductors at one end must, however, considerably reduce the rating of the machine. Where the turbo-generator is very long, it is better to pass the air through the iron from both ends to the middle, or to adopt the method illustrated in Fig. 216.

It will be seen on page 242 that the value of h_v (the watts per sq. cm. per degree C. difference of temperature between surface and air) is dependent upon the v, and as it is the velocity of the air in intimate contact with the surface that is of chief importance, we may gather that for a *given quantity of air passed through the machine* narrow ducts will be more effective than wide holes. The ducts, however, must not be too narrow or they will be liable to be stopped up by the accumulation of dirt. If a duct is too wide the air passes through it without taking as much heat from the iron as it would if it were passed through a narrower duct. Experiments

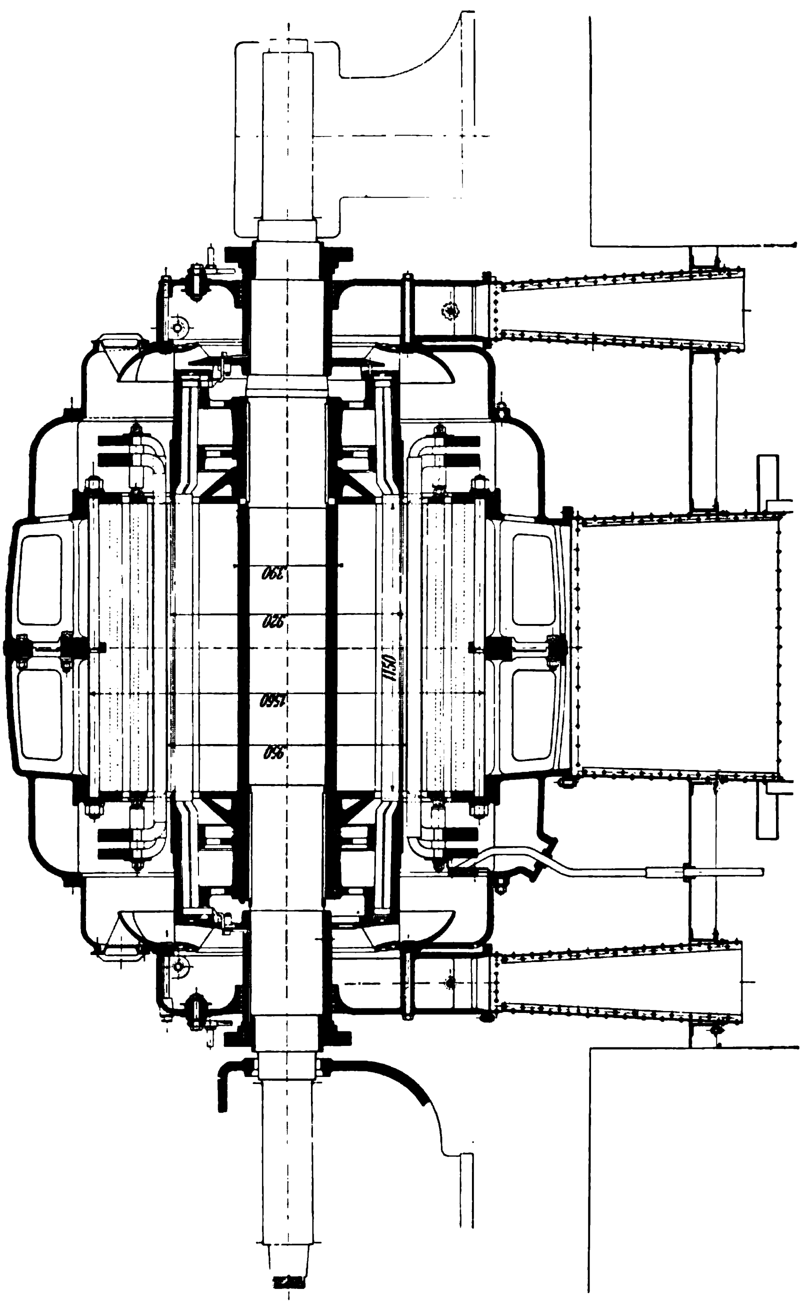


FIG. 220.—Single-phase turbo-generator by Siemens-Schuckert, 1670 K.V.A., 6800 volts, 25 cycles. In the system of ventilation here shown, the air is drawn in mainly at the left-hand end and forced through axial holes to the right-hand end. To draw the air through the rotor there is an expelling fan at the right-hand end. More cold air is here drawn in and expelled through the annular space around the frame.

have been made from time to time to determine what is the **best width of duct,** and it seems to be generally agreed that for large machines having great depth of iron the air ducts should be about 1 cm. wide. If, however, the pressure available for forcing the air through is high enough, and especially if the air is filtered so that there is not so much danger from dirt, it seems to be better to choose a rather narrow duct. It will be seen that in the 15,000 K.V.A. machine illustrated in Fig. 367, we have ducts only $\frac{5}{16}''$ wide. As the machine is ventilated by means of filtered air from an independent blower at a considerable pressure, rather narrow ducts can be used. We are thus able to use a very large number of ducts without taking up too much room, and these present an enormous cooling surface.

Yet another method which is very effective for long turbo-generators is illustrated in Figs. 221 and 221*a*. There the ducts are made much like radial ducts with ventilating plates; but the spacers in the plates are not radial. They consist of concentric ribs which allow the air to enter at the top of the machine and go out at the bottom. The supply of air may either come from fans in the rotor shaft, as shown in Figs. 215 and 220, or from an independent blower. The air that cools the rotor is drawn through channels in the shaft by means of the centrifugal action of the ventilating ducts, and is expelled into the air-gap, from whence it passes through a certain section of ventilating ducts in the stator provided for it at the lower part of the stator. Channels are provided under the floor for both the incoming and the outgoing air.

The **design of the ventilating ducts** themselves is a matter upon which much ingenuity has been expended. The **ventilating plate** which serves to separate the stampings, though it should be cheap to manufacture, must be made of such substantial design that it will not be crushed by the pressure on the punchings, and will not have any parts that can get loose and fly out. At one time ventilating plates with the spacers punched up were largely in use; but these are not satisfactory, unless the metal is thick enough to obviate all risk of the squeezing over of the punched-up part. Most manufacturers now prefer to rivet spacers of substantial construction on to an iron punching. There is seldom any advantage in giving to the internal parts of the spacers the shape of blades in a turbine. The shaping of the spacers to imitate the blades of a turbine can only be of advantage if the air is being accelerated in a tangential direction by the spacers themselves. Very often the air receives its main tangential velocity from the spider arms, and the shaping of the spacers is in this case of very little use. Most standard machines have to be designed for rotating in either direction, so that in these it is best to have the spacers radial (see Fig. 511).

TABLE XI. POWER TAKEN TO DRIVE FANS.

Diameter of fan in centimetres.	Outside diameter of frame in centimetres.	Smallest opening in path of air in sq. cms. (outlet).	Speed of fan blade at 1000 R.P.M. in metres per second.	Watts taken to drive fan.	Maximum watts carried away by air for 40° C. rise of machine.	Efficiency of fanning action.
25	50	250	13	40	2,500	12 per cent.
37·5	75	550	19	140	9,500	18 „
50	100	1,000	25	500	22,000	18 „

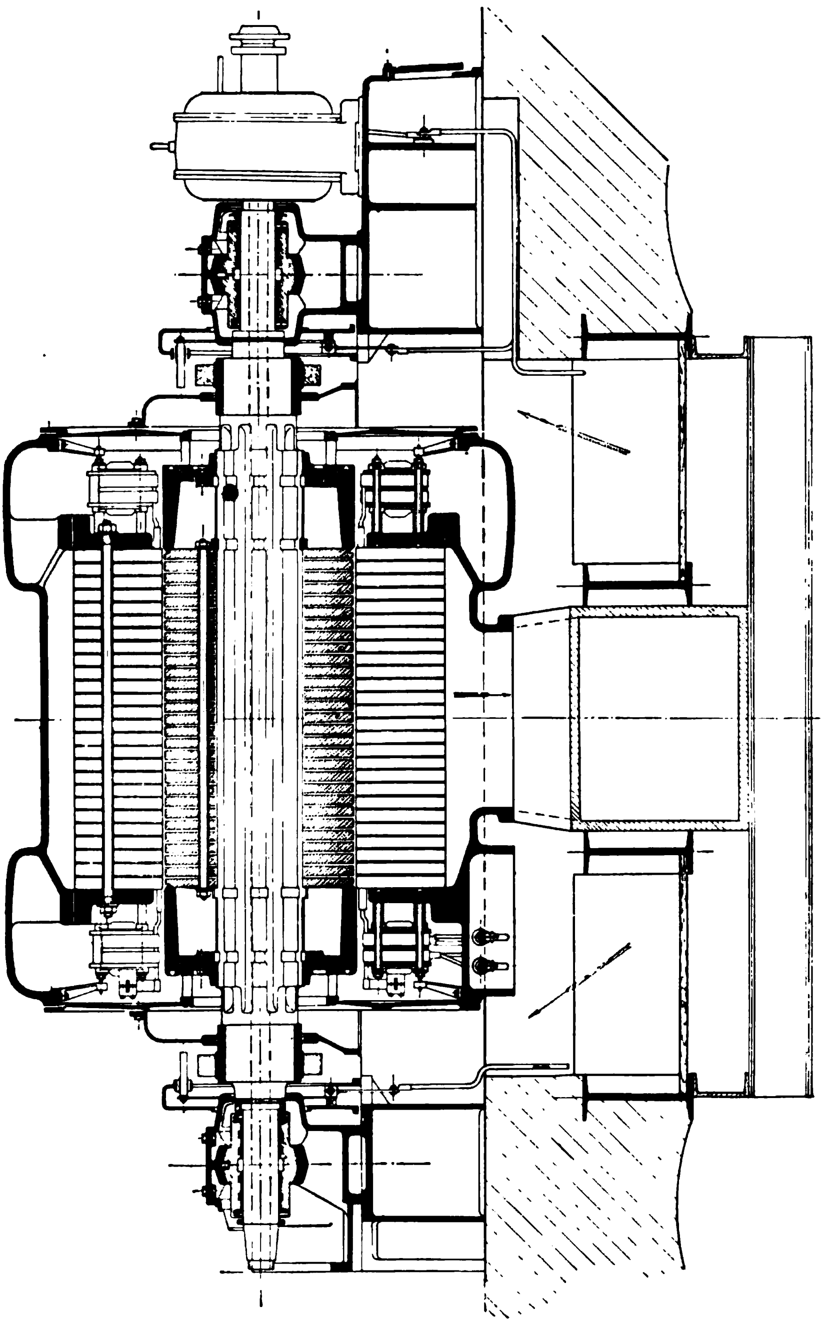


FIG. 221.—1250-K.V.A., 1000-volt, 3-phase turbo-generator, by Felten & Guilleaume-Lahmeyerwerke, running at 3000 R.P.M.

Power taken to drive the fan. The amount of power taken to drive a fan such as that illustrated in Figs. 215, 218 and 429 depends very largely upon the amount of air passing through it, and this again depends upon the openings provided for

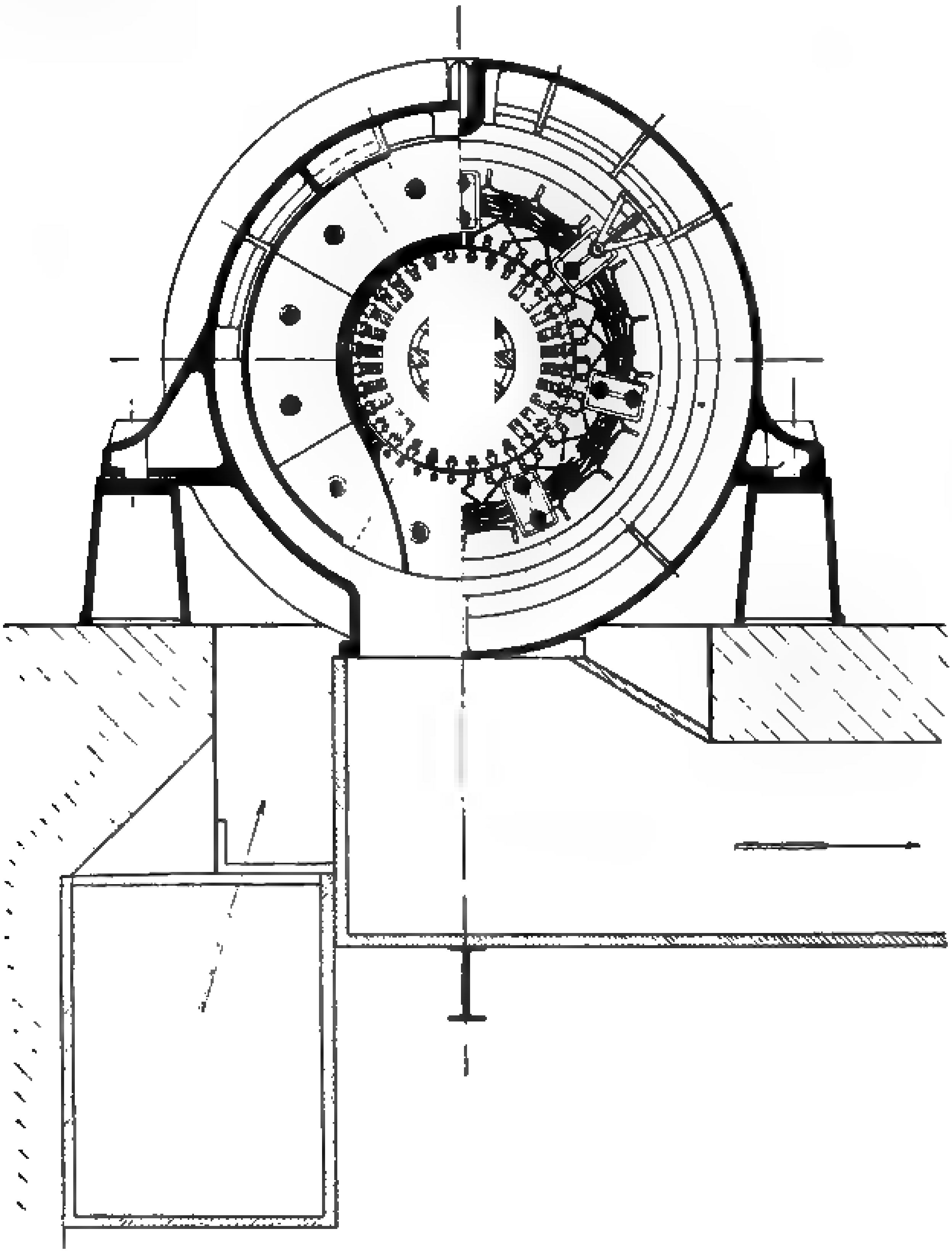


FIG. 221a.—1250-K.V.A., 1000-volt, 50-cycle 3-phase turbo-generator by Felten & Guilleaume-Lahmeyer. The scheme of ventilation is one in which the air passes from the top of the stator along concentric ducts between the stator punchings, and is expelled at the base, as will be seen from the longitudinal section. Scale 1 : 25.

the air. In many cases the air is throttled mainly at one place; it may be at the entrance or the exit openings, and often the amount of air is controlled by the size of these openings. In order to give some idea of the amount of power taken to drive fans on machines of various sizes, we quote in Table XI. figures for three frames of different sizes.

For a given maximum velocity of air, the power taken will vary directly as the amount of air supplied. But for an outlet of given size, the power taken will vary as the cube of the amount of air supplied per second, because the pressure required varies as the square of the velocity. For a given number of revolutions per minute, the power taken will vary as the 3·5th power of the diameter of the fan, other dimensions remaining constant. For a given fan the way that the power will vary as the speed is increased depends upon how far the air is throttled. If it is completely throttled (on machines having only small openings for the air and

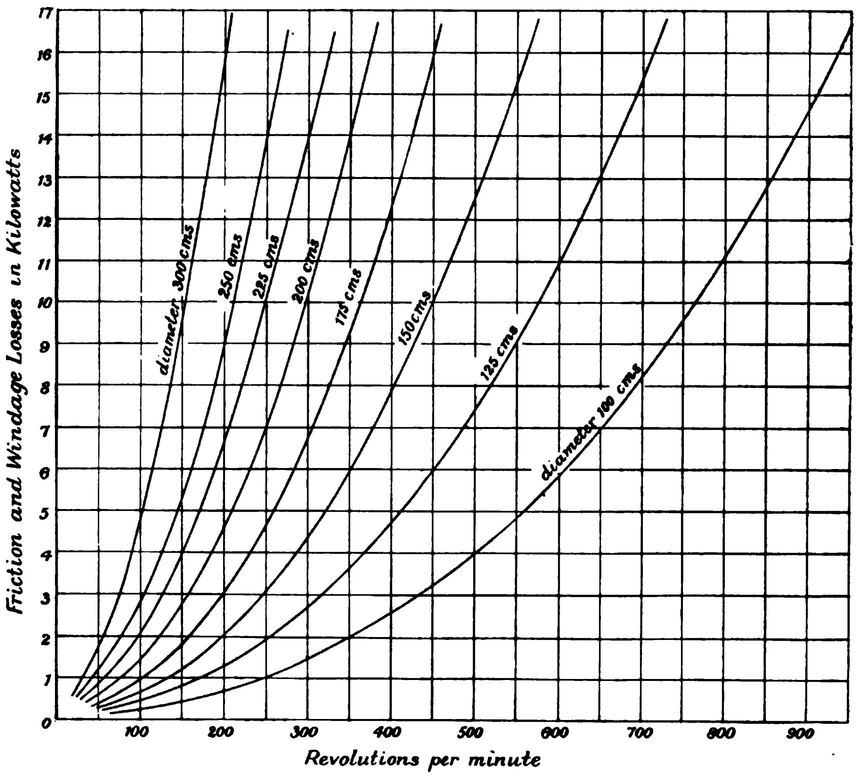


FIG. 222.—Approximate values of friction and windage on engine-driven salient-pole A.C. generators; $l=30$ cms.

a fairly big fan it is almost completely throttled), the power taken varies nearly as the square of the velocity. Where the passage for the air is free, it varies nearly as the cube of the speed of the fan.

The following approximate data are sometimes useful. One watt will give a rise of 1° C. to one gram of air per second. One lb. of air per second requires 453 watts to raise it 1° C. The volume of one pound of air is

$$12\cdot5 \times \frac{273 + \text{C.}^\circ}{273} \times \frac{760}{\text{m.m.}} \text{ cubic feet,}$$

where m.m. denotes the barometric pressure in millimetres of mercury and C.° denotes the temperature of the air. If we take the volume of the air

at 35° C. we get the following rule for calculating the amount of air required for cooling:

$$\text{Cubic metres per second} = \frac{\text{watts lost}}{\text{temp. rise of air} \times 1130}.$$

One generally allows 50 % more than this in cases where some of the air comes out at a temperature lower than the maximum.

Friction and windage losses. It may be as well to deal here shortly with friction and windage losses. It is impossible to compute accurately these losses, because such small variations in the design often make great differences, especially

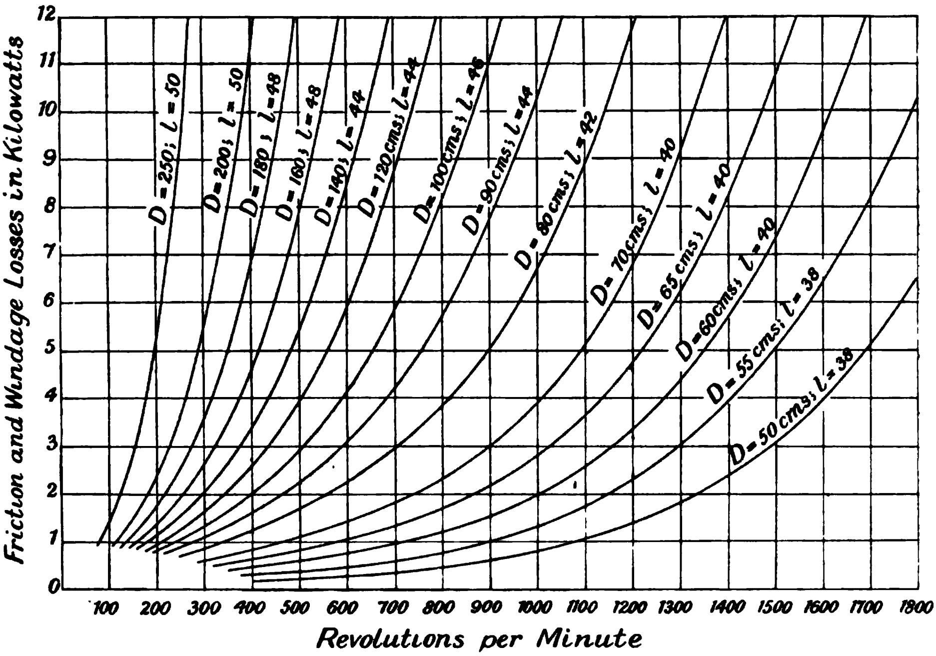


FIG. 223.—Approximate values of friction and windage of rotors of induction motors.

in windage losses. Still, as the electrical designer has so often to fill in an approximate figure for the friction and windage losses in calculating efficiency, it is well to have a few curves such as those given in Fig. 222 and 223 to aid him. Fig. 222 relates to the friction and windage losses of engine-driven generators. These are based on tests upon 50-cycle generators having an axial length of 30 cms.

It is found that the windage of the rotor of an induction motor as ordinarily constructed is less than the windage of a salient pole generator of the same diameter and length. The friction of the bearings is also rather less, because these are not so massive as generator bearings. Figure 223 gives us rough figures for the friction and windage of induction motors of standard design. The curves are marked with the diameter and axial length of the motors.

These curves are only intended to give one a rough idea of the friction and windage on a machine. The only accurate way of determining these is by actual measurement.

CHAPTER X.

THE PREDETERMINATION OF TEMPERATURE RISE.

THE determination of the temperature rise of any part of an electrical machine from the design data and a supposed knowledge of the conditions under which it is worked, will always be a difficult matter; and no very great accuracy can be expected from such calculations, because of the impossibility of telling beforehand exactly what the losses will be, or of predetermining with accuracy the cooling conditions.

Nevertheless, it is worth while to make a very close study of the ways in which the heat generated in the iron or copper is carried away, and to make our rules for the quantitative determination of the amount of heat passed from one part of the machine to another as accurate as they can be under the circumstances. Such a study generally leads to a knowledge of defects in the design which can be remedied. There is no doubt that the great increase in the output per lb. of material that has been made during the last few years in running machines has been obtained more by improvements in the methods of cooling than in the reduction of the losses. The heat produced in any part has a definite path from the point of origin to the place where it is thrown out from the machine. Thus some of the I^2R losses in the armature conductors may have only to pass through a certain thickness of insulation to the air surrounding the coils; while the heat generated in the copper in the slots passes through the insulation to the iron, where it meets with the heat produced in the iron, and both together are conducted to the ventilating ducts and carried by the air to the exterior.

We can imagine lines of heat flow drawn through the machine which follow everywhere the paths of the heat from the point of origin to the point of discharge. At some points there may be constrictions in the path which it is desirable to avoid; at others the heat stream flows easily without undue temperature gradient. Everywhere, at right angles to the lines of heat flow, we can imagine isothermal surfaces constructed which enclose the points of highest temperature.

In those parts of the machine where there is a heavy temperature gradient, that is to say, where the isothermal surfaces are crowded together, the designer must consider what can be done to open these surfaces out, and lower the internal temperature.

We propose to give in this chapter rules which will enable us to calculate the amount of heat carried from one part to another under given conditions.

We will have to deal with the passage of heat by conduction, by convection, and by radiation.

Conduction of heat. It is well to have mental pictures of the relative heat conductivity of the different materials with which the designer has to deal.

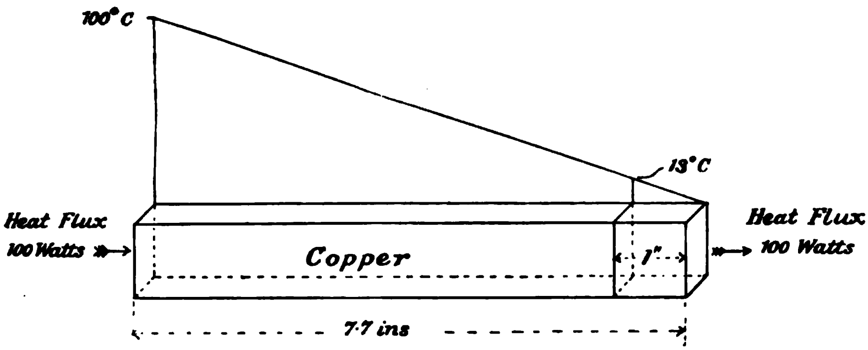


FIG. 225*a*.

Figs. 225*a*, *b*, *c* and *d* show the heat conductivity of copper, iron, paper and baffled air.

In these figures the heat-flow is given in watts, that being the most convenient way of measuring it for our purpose.*

In Fig. 225*a* we have a copper bar 7·7 ins. long and of 1 sq. in. section. It is supposed that the bar is surrounded by a perfect heat insulator (or it may be by other bars having the same temperature distribution), so that no heat escapes at the sides. If heat flows in at one end at the rate of 100 joules per second (*i.e.* 100 watts), the temperature gradient in the bar will be as depicted in Fig. 225*a*. Two points 7·7 inches apart have a difference of temperature of 100° C. There is a difference of temperature of 13 degrees for points 1 inch apart in the line of the flow of heat. That is to say, a difference heat potential of 13° C. will drive 100 watts across an inch cube of copper.

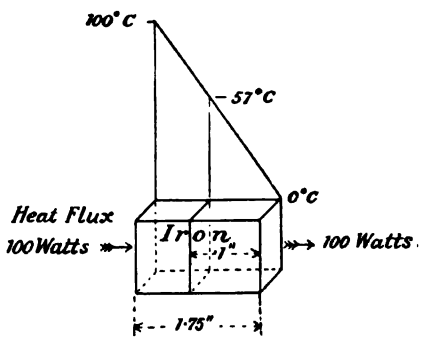


FIG. 225*b*.

Fig. 225*b* shows a wrought-iron bar of the same cross-section, along which 100 watts is passing by heat conduction. It will be seen that the temperature gradient is more than 4 times as steep. In a bar of cast iron the gradient would be much steeper still. The conductivity of

* The relation between the heat units is as follows: 1 gram calorie (the heat required to raise 1 gram of water 1° C.) is equal to 4·2 joules or 4·2 watt seconds, so that the passage of 1 calorie per second through any given surface is equivalent to the passage of 4·2 watts through that surface.

cast iron depends greatly on the nature of the crystallization. Common cast iron has only one-half the conductivity of wrought iron.

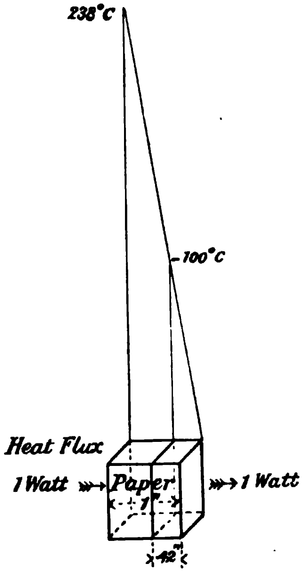


FIG. 225c.

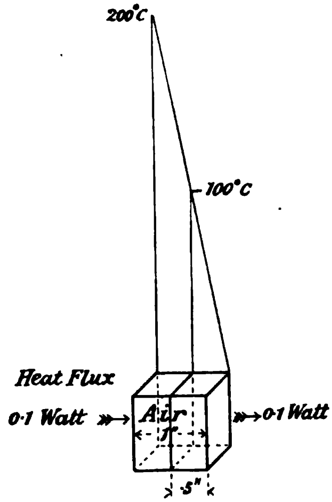


FIG. 225d.

TABLE XII. HEAT CONDUCTIVITY OF METALS.

Material.	Thermal Conductivity. Per square centimetre per ° C. of difference of temperature per centimetre length of path.		Per square inch per ° C. of difference of temperature per inch length of path.
	In calories per second.	In watts.	In watts.
Copper. (See note on p. 229) - -	0·72 to 1·0	3·0 to 4·2	7·6 to 10·6
Steel punchings along laminations -	0·15	0·63	1·6
Steel punchings across laminations (10 per cent. paper insulation) - -	0·0028	0·0118	0·03
Steel punchings across laminations (8 per cent. paper insulation) - -	0·0035	0·015	0·038
Steel punchings across laminations (7 per cent. varnish insulation) - -	0·0061	0·026	0·065
Cast iron - - - - - - -	0·03 to 0·06	0·125 to 0·25	0·32 to 0·64
Brass - - - - - - -	0·2	0·84	2·14

Fig. 225c represents the case where heat flows through pressed paper insulation at the rate of one watt per sq. in. Here the temperature gradient is exceedingly steep, although we are only passing 1 watt per sq. in., instead of 100 watts, as in the cases depicted of metal bars. If, instead of solid pressed paper, we have a number of sheets of paper with layers of air in between them, the temperature

gradient will be steeper still. One of the worst heat conductors known is air which is prevented from circulating by being mixed with some finely divided fabric.

Fig. 225*d* shows the temperature gradient in baffled air. Here we have again had to reduce the heat flux (this time to 0·1 watt), to make the figure on a reasonable scale. It will be seen that the heat conductivity of paper is $8\frac{1}{2}$ times as great as the heat conductivity of baffled air.

Tables XII. and XIII. show the heat conductivity of various materials used the construction of electric machines.

TABLE XIII. HEAT CONDUCTIVITY OF INSULATING MATERIALS.*

MATERIAL.	HOW MOUNTED.	THERMAL CONDUCTIVITY. Per square centimetre per ° C. of difference of temperature per centimetre length of path. In calories per second. (1)	Per square centimetre per ° C. of difference of temperature per centimetre length of path. In watts. (2)	Per square inch per ° C. of difference of temperature per inch length of path. In watts. (3)
			λ_h	λ''_h
Varnished cloth (empire cloth)	16 turns, each 0·0175 cm. thick, very tightly wrapped	0·0006	0·0025	0·0063
Press-spahn, untreated	2 pieces, each 0·16 cm.	0·00041	0·0017	0·0042
Rope paper, untreated	24 turns, 0·014 cm. thick, tightly wound	0·00028	0·0011	0·0029
Rope paper and oil -	24 turns, 0·014 cm. thick, tightly wound	0·00034	0·0014	0·0037
Rope paper, treated with sterling varnish	Successive turns, 0·019 cm. thick, tightly wrapped	0·00040	0·0017	0·0042
Fullerboard, varnished	Successive turns, 0·028 cm. thick, tightly wound	0·00034	0·0014	0·0035
Empire cloth and mica	Alternate turns of empire cloth, 0·018 cm. thick; and mica, 0·075 cm. thick, tightly wound	0·00050	0·0021	0·0053
Empire cloth, mica and tape	As in Fig. 227, containing some air spaces	0·00036	0·0015	0·0038
Paper and mica -	Allowing for some looseness in slot	0·00029	0·0012	0·0031
Pure mica - -	3 pieces, each about 0·13 cm. thick	0·00087	0·0036	0·0091
Built-up mica - -	Micanite tube containing 19 per cent. shellac	0·00025	0·0010	0·0026
Built-up mica - -	Micanite tube containing 11 per cent. shellac	0·00029	0·0012	0·0031
Linen tape, treated -	Treated in insulating varnish and baked	0·00035	0·0014	0·0037

* For method of testing and further particulars see "Heat Paths in Electrical Machinery," Symons & Walker, *Journ. Inst. Elec. Engrs.*, vol. 48, p. 674.

The amount of current that an armature coil or field coil will carry without exceeding its guaranteed temperature rise greatly depends upon the heat conductivity of the materials with which the coil is insulated, and upon the way in which they are applied. In many cases it is well to make a rough calculation of the number of watts per square centimetre which can be passed through the insulation employed under the running conditions. The important factors involved are:

(1) The nature of the insulation and its heat-conducting qualities.
(2) The thickness of the insulation.
(3) The resulting difference in temperature between the copper and the iron.

Where the insulation is well pressed and in close contact with the copper and the iron, the temperature gradient within it will be fairly definite and of a known amount, depending on the material. If λ_h is the heat conductivity expressed in the units employed in the second column of Table XIII., the formula connecting the various quantities is as follows:

Watts per sq. cm. passing from copper to iron

$$=\lambda_h \times \frac{\theta_1 - \theta_2}{c},$$

where c is the thickness of insulation in centimetres, θ_1 the temperature of the copper in degrees Centigrade and θ_2 the temperature of the outside of the coil.

EXAMPLE 29. An armature coil of an induction motor is insulated by means of a tube of built-up mica, 0·3 cm. in thickness, which fits tightly in the slot. If the permissible running temperature of the copper is 75° C. and the temperature of the iron is 55° C., how many square centimetres per watt must we allow for the cooling of the coil?

$\theta_1 - \theta_2 = 20°$ C. From Column 2, Table XII., we may take $\lambda_h = 0{\cdot}001$.

$$\text{Watts per sq. cm.} = 0{\cdot}001 \times \frac{20}{0{\cdot}3} = 0{\cdot}067 \text{ watt per sq. cm.}$$

For a mixture of paper and mica (half and half by volume), and allowing for the average amount of looseness which occurs in a well pressed coil, the constant λ_h may be taken at 0·0012 in cm. measure. In inch measure λ_h'' is 0·0031.

EXAMPLE 30. An armature coil is wrapped on the straight part, which lies in the slot, with paper and mica in equal proportions to a thickness of 0·06″. For a difference of temperature of 22° C. between iron and copper, how many watts per sq. in. will pass through this insulation?

$$\text{Watts per sq. in.} = 0{\cdot}0031 \times \frac{22}{0{\cdot}06} = 1{\cdot}2 \text{ watts per sq. in.}$$

This would be the usual allowance for a direct-current armature coil insulated for 500 volts.

EXAMPLE 31. A long coil consists of four conductors each 0·6″ × 0·3″ (say 0·175 sq. in. area). The wall of insulation consists of 0·125 inch of paper and mica in equal parts well pressed and making a reasonably good fit in the slot. If the copper is worked at 2000 amps. per square inch, what will be the difference of temperature between iron and copper?

The resistance of the conductors when hot will be about 0·00054 ohm per foot. The current per conductor will be 350 amps. The total loss per foot run will be

$$350 \times 350 \times 0{\cdot}000054 \times 4 = 26{\cdot}4 \text{ watts.}$$

The mean area of the insulation of a foot run will be

$$4{\cdot}8'' \times 12'' = 57{\cdot}5 \text{ sq. in.} \qquad \frac{26{\cdot}4}{57{\cdot}5} = 0{\cdot}46 \text{ watt per sq. in.}$$

$$0{\cdot}46 = 0{\cdot}0031 \times \frac{\theta_1 - \theta_2}{0{\cdot}125}.$$

Therefore $$\theta_1 - \theta_2 = 18{\cdot}5° \text{ C.}$$

In the above examples we have made allowance in the value of λ_h for the air spaces occurring in the insulation. In cases where hard-pressed insulation, such as press-spahn is employed, there will usually still be a small air space between this and the adjacent metal, and often this air space is a greater hindrance to the

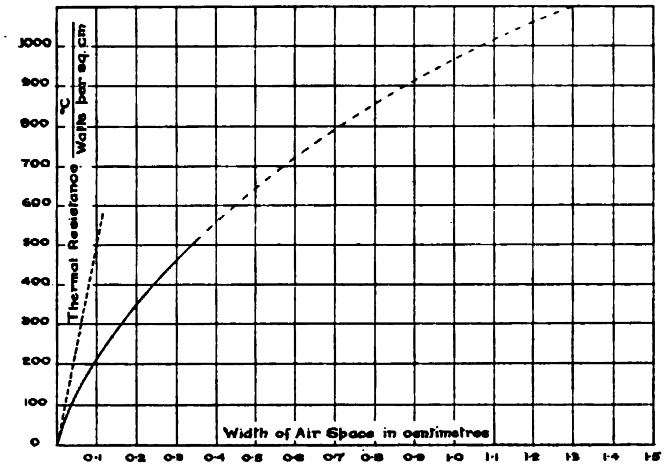


FIG. 226.—Thermal resistances of air spaces of different thicknesses.

passage of heat than the solid insulation. If the amount of air space is known, or can be approximately guessed, its thermal resistance can be allowed for by taking values from the curve given in Fig. 226.

EXAMPLE 32. Suppose that we have a field coil which is insulated on the inside next the pole with treated fullerboard of a thickness of 0·2 cm. From Table XIII. we find that the thermal conductivity of this material (in watts per square centimetre, etc.) is 0·0014. The thermal resistance of 1 sq. cm. is there $0{\cdot}2 \div 0{\cdot}0014 = 142$, so that if there were no air space and we were passing to the pole 0·15 watt per square centimetre, the difference in temperature of pole and coil would be only 21·3° C. If now we introduce an air space of 1 mm., whose resistance from Fig. 226 is about 200, the total resistance is raised to 342 and the difference in temperature for the same heat flow would be 51·5° C.

Of all the materials used in the insulation of armature and field coils, pure mica in its original crystalline form is the best heat conductor. If, however, the mica is split up into laminae, and built up in the form of micanite, the thin layers of shellac and air enormously increase the thermal resistance, so that built-up mica is a rather worse heat conductor than many of the fibrous insulating materials. Indeed any heating or bending of the pure mica, which will interfere with its solidity, will greatly increase its thermal resistance.

In Table XIII. it will be seen that in several cases (such as the first item) the thermal conductivity is given for very tightly wrapped material. For the experiments in which the conductivity was measured, the material was specially wrapped with great care, so as to exclude practically all the air spaces; so that the values in these cases must be taken as the maximum obtainable, and must not be used in practical calculations unless the construction is such as to exclude all air. The impregnation of coils greatly improves the heat conductivity by filling up air spaces.

Sometimes coils are impregnated with petroleum residue before the main slot insulation is wound on. This ensures good heat conductivity up to the inside surface of the insulation, but we have still some air spaces between the layers of insulation which are wrapped on, and there must necessarily be some little space here and there between the outside of the insulation and the walls of the slots. In the machine, the test of which is described below, the insulation was of this type.

EXAMPLE 33. A test was made on a 5000 K.W. three-phase generator by means of thermo-couples placed in the armature coils during the course of construction. Fig. 227 shows the arrangement of the armature coils; the position of the thermo-couples is indicated by the letters R, S, T, U, V. Junction R gave the temperature of the copper inside the slot; S the temperature of the iron surrounding the slot; T the temperature of the outside of the coil on the part exposed to the air; U the temperature of the copper in part of a coil projecting 6 in. from the iron; V the temperature of the copper in part of a coil projecting 9 in. from the iron. The generator was run at full speed with the armature short circuited, the field current being increased until the armature current was 328 amperes. The run was continued until the temperatures of all parts were constant. The table below gives the degrees, rise above the temperature of the air admitted to the machine (23° C.).

° C. Rise.
$R = 39{\cdot}0$
$S = 18{\cdot}4$
$T = 24{\cdot}6$
$U = 38{\cdot}0$
$V = 34{\cdot}4$

Fig. 227 gives the arrangement of the conductors and insulation in the slot. It is drawn full size. Each conductor, which consisted of two copper straps each 0·45 in. × 0·2 in., was insulated with tape and mica, a piece of mica 0·03 in. thick being added as a spacer. All four conductors were impregnated in vacuo and wound over with empire cloth and mica to a thickness of 0·13 in. The whole was then wound with linen tape. The total thickness of insulation amounted to 0·177 in. The various insulating materials were then present in the following proportions: Empire cloth, 0·07; mica, 0·03; varnish and air, 0·02; paper, 0·017; tape, 0·04. The heat conductivity of the insulation is easily calculated from the above figures. The total loss in the copper conductors per foot run of coil was 27·2 watts. In calculating this, allowance has been made for the rise in temperature of the copper and for eddy currents* produced in the conductors. The difference of temperature between the copper and iron is 20·6° C. Mean perimeter 5 in., so that the total area of insulation per foot run is 60 sq. in. With 27·2 watts per foot run this gives just over 2·2 sq. in. per watt. The specific conductivity for heat of the insulation works out at 0·00153 watt per centimetre cube per degree. This conductivity is considerably lower than the figure (0·002) found from tests on empire cloth and mica wound on a copper cylinder with the fewest possible air spaces, as can be easily understood.

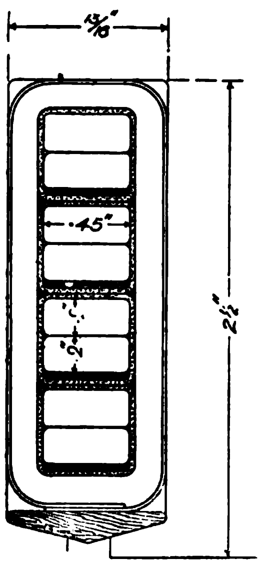


FIG. 227.—Arrangement of insulation in heat conductivity test.

With coils of rectangular section wrapped with empire cloth and mica, or paper and mica, in the ordinary method, one may expect to have a heat conductivity

* See paper by A. B. Field, *Journal of the American Institute of Electrical Engineers*, July, 1905.

not higher than 0·0015 watt per cubic centimetre per degree, and for thin insulation, such as used for 500-volt machines, one may take the figure for λ_s as

FIG. 228.—Positions of thermo-couples for test on the heating of armature coils.

0·0012 to allow for the relatively greater importance of looseness in the slot. This in inch measure gives us $\lambda_s'' = 0{\cdot}0031$. For instance,

EXAMPLE 34. On the armature of a direct-current generator whose conductors were insulated with manilla paper and mica to a thickness of 0·16 cm., the temperature rises after a full-load run under conditions which made the square inches per watt 0·9, were as follows: Internal copper, 41°; iron, 22°. If we use the figure 0·0012 watt per cubic centimetre per degree, we would obtain a temperature rise of copper above iron of 23°.

Conduction of heat along conductors. It sometimes happens that the copper conductors on an armature or field-magnet are grouped together so closely that very little air can circulate between them, and the total cooling surface of the group is too small to dissipate the heat generated in it. In this case one relies mainly for cooling upon the conduction of heat along the conductors to parts of the coils where the cooling conditions are better. A good illustration of this case is offered by the end windings of a two-pole field-magnet for a turbo-generator, such as is shown in Fig. 359. These end windings are completely covered in by a

steel end bell, so that in any case the air would not circulate well between individual coils, and to avoid the accumulation of dirt it is sometimes found advisable to fill the interspaces with suitable insulation. A great proportion of the heat generated in these end windings is conducted along the copper into the parts of the coils lying in the slots, and from thence it is conducted into the iron of the field-magnet.

The flow of heat from the centre of the coil to the cooler parts can only occur if there is a considerable temperature gradient in the end windings. It is necessary sometimes to calculate what this temperature gradient will be, and what the maximum temperature rise will be in the centre of the group. The problem is somewhat complicated by the fact that the resistance of copper changes with temperature, and one ought to take account of this change of resistance because it makes the

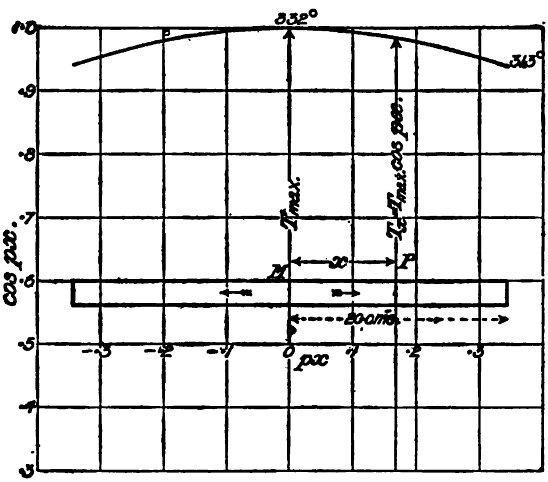


FIG. 229.

watts lost increase according to a compound interest law. Moreover, in most cases that arise in practice, part of the heat is radiated from the surface of the coils, and part is conducted along them.

We will first take the case where a conductor is so surrounded by other conductors at the same temperature as itself that the whole of the heat generated in it is conducted to the cooler ends, and none passes to the sides. Afterwards we will take the case where a considerable fraction of the heat passes out to the sides and the remainder along the conductor.

Let M be the centre point of a symmetrically situated end connector so surrounded by other conductors that all the heat generated by electric current in it passes to the ends. M is supposed to be the hottest point, and from it heat flows to the right and to the left as indicated by the arrows. It is sufficient to investigate the distribution of temperature on one side, say the right side. Let the distance in centimetres of any point P from M be denoted by x. Let the cross-section of the conductor be 1 sq. cm., so that the volume of any element of length dx is dx cubic centimetre. Now, as the resistance of copper is almost proportional to

its temperature measured from an artificial zero 240° C. below 0° C., the resistance of a centimetre cube may be taken to be:

$$R=\frac{1{\cdot}6\times 10^{-6}\times T}{240},$$

where T is its temperature in ° C. above the artificial zero.

If I_d is the current density in amperes per square centimetre, the loss per cubic centimetre will be:

$$I_d^2 R = I_d^2 \times \frac{1{\cdot}6\times 10^{-6}\times T}{240}.$$

The amount of heat passing through the centimetre of cross-section at the point P will be the sum of all the heat produced between M and P—that is to say:

$$I_d^2 \times \frac{1{\cdot}6\times 10^{-6}}{240}\int_0^x T\,dx.$$

Now the heat conductivity * of copper is such that when there is a difference of temperature of 1° C. between opposite sides of a centimetre cube, the flow of heat through the centimetre arrear is equivalent to the heat produced by 3 watts (see Fig. 230). Therefore three times the temperature gradient gives us the heat flow per square centimetre in watts. As x increases the temperature decreases, so that $\frac{dT}{dx}$ is negative. Thus we have

$$-3\frac{dT}{dx} = I_d^2 \times \frac{1{\cdot}6\times 10^{-6}}{240}\int_0^x T\,dx.$$

We may take as a solution: $T = T_{max}\cos px$.

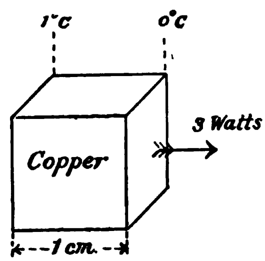


FIG. 230.

In cases which we work out in practice the angle px never assumes values which make $\cos px$ negative, so that T is always positive. If T were negative it would be below the artificial zero. The above solution would only be wholly true if the resistance of copper were negative below this artificial zero.

The distribution of temperature in a conductor such as we have supposed is therefore given by the top part of a cosine curve, as shown in Fig. 229.

The value of p is

$$\sqrt{I_d^2\times\frac{1{\cdot}6\times 10^{-6}}{3\times 240}} = 4{\cdot}71\times 10^{-5}\times I_d.$$

Therefore $\qquad T_x = T_{max}\cos(4{\cdot}71\times 10^{-5}\times I_d\times x),$*

where I_d is the current density in amperes per square centimetre,

x is the distance from the hottest point in centimetres,

T_x is the temperature, above $-240°$ C., at any point x,

T_{max} the temperature, above $-240°$ C., at the hottest point.

An example will make this clearer. Suppose that we have a hot-bed of conductors so bulky that we can assume that the centre conductor parts with no heat laterally. All heat generated in it passes by conduction to points 20 cms. away

* As the authorities differ as to the heat conductivity of copper, the author has taken a value given by Lorenz, which appears to be on the low side. Tests made by the American Westinghouse Company indicate that the figure 3·8 watts per sq. cm. per ° C. per cm. is more nearly correct. This would give the formula:

$$T_x = T_{max}\cos(4{\cdot}2\times 10^{-5}\times I_d\times x).$$

from the centre, which we will suppose are maintained at 40° C. Each conductor is 0·1 sq. in. section, and carries a current of 250 amperes. What is the temperature of the hottest point?

$$I_d = 388 \text{ amperes per square centimetre.}$$
$$T_s = (40 + 240) = 280.$$
$$280 = T_{max} \cos(4{\cdot}71 \times 10^{-5} \times 388 \times 20).$$
$$280 = T_{max} \cos 0{\cdot}366 = 0{\cdot}935\ T_{max}.$$
$$T_{max} = 300.$$
$$300 - 240 = 60^\circ \text{ C. is the temperature of the hottest point.}$$

Now consider the case where part of the heat generated is radiated from the surface of the group of conductors, and part is conducted to the ends. In the cases which occur in practice there is a certain specified temperature on the outside

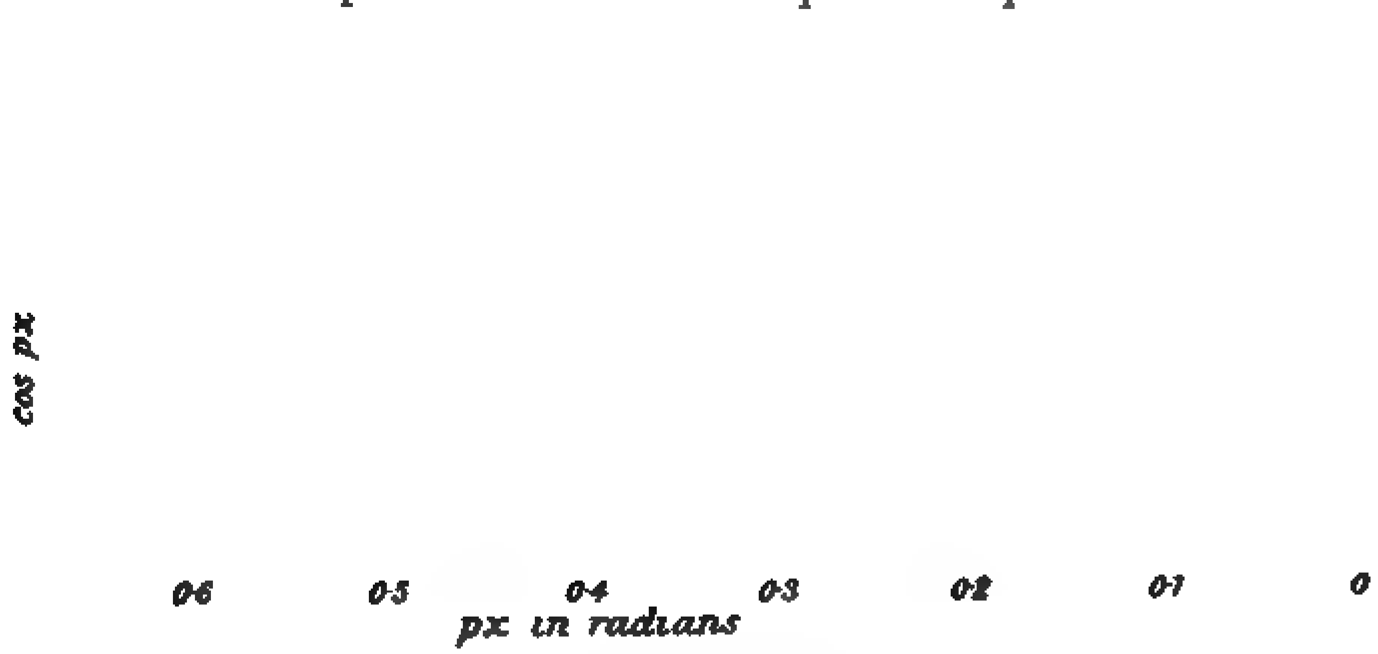


Fig. 231.

of groups of coils which must not be exceeded. Assuming in the first instance that the temperature is reached, we can roughly estimate the number of watts per square centimetre which will be dissipated from the surface, having regard to the thickness of insulation and the amount of air circulation. Let W represent the total watts lost in the group of conductors, and w the watts dissipated from the surface. Then $W - w$ will be the heat watts conducted along the copper. The temperature rise of the hottest point will be lower than if no heat were lost laterally. Let us say that the temperature rise is the same as it would be if the current density were reduced from I_d to I_e and no heat were lost laterally.

Then $$\frac{I_e^2}{I_d^2} = \frac{W - w}{W}.$$

From the value of I_e thus obtained we can as a first approximation find the temperature of the hottest point by the foregoing formula, and get a fair idea of the mean temperature of the whole cooling surface. We can then make a more accurate estimate of w, and, if necessary, recalculate I_e, and from it T_{max}.

For convenience in obtaining the values of $\cos px$ from the values of px expressed in radians, it is well to have a curve such as that plotted in Fig. 231.

Conduction of heat along poles. It is sometimes useful to make an estimate of the amount of heat conducted away along an iron pole piece. In most cases only the roughest estimate can be made of this, because the distribution of temperature is usually too complex for us to get accurate data with which to start our calculation. If, however, we begin with the assumption that a certain total number of watts will pass through the internal insulation of the field coil for a certain average temperature of the pole pieces, we can arrive at a rough estimate of the temperature of the pole surface necessary to dissipate these watts to the air by the methods considered under the heading "Cooling by air." Having now provisionally fixed the average temperatures of the surfaces where the heat is received, and where it is discharged, it is an easy matter to calculate whether the difference of temperature is sufficient to drive the heat along the pole. If it is not, then we must correct our assumption as to the amount of heat coming from the coil, coming nearer at each trial the average temperature of the inside surface of the insulation and the radiating surface of the pole.

Cooling by air.* There are three main cases occurring in electrical machinery in which it is necessary to calculate the rate of convection of heat from a solid surface to the surrounding air.

(1) We have the case of an armature or field-magnet of approximately cylindrical shape revolving within the stationary part of the machine. (Cooling coefficient denoted by h_y.)

(2) We have the case of a field coil against which a draught of air is blowing. (Cooling coefficient denoted by h_d.)

(3) We have the case of the iron surface of a ventilating duct, through which the air is passing at a certain velocity. (Cooling coefficient denoted by h_v.)

The laws of cooling of the solid surface are different in the three cases. The first case (the cooling of the revolving cylinder) is very complicated. A formula for the close predetermination of temperatures would have to take into account, not only the square inches per watt and the peripheral speed, but also the length of the air-gap, the temperature and shape of the surrounding objects, as well as of the air, the nature of the cooling surface, and the rate at which the air in the gap is changed by artificial ventilation.

For ordinary direct-current armatures surrounded by ordinary field-magnets with normal air-gaps, and with no more interchange of air than is naturally produced by the rotation of the armature, the formula given by Kapp,

$$t^\circ = \frac{550}{\frac{O}{W}(1 + 0{\cdot}1v)},$$

gives good practical results. Here O is the area of the cylindrical surface, W the watts to be dissipated, v the peripheral velocity in metres per second, and t° the degrees Centigrade rise above the surrounding air.

* For the amount of air required and the various methods of ventilation, see page 206 *et seq.*

Where we are dealing with a cylindrical cooling surface consisting of iron punchings only, the coefficient (550 in the above formula) should be given a rather lower value. Perhaps the formula,*

$$(1) \qquad t^\circ = \frac{333 \times \text{watts per sq. cm.}}{(1+0{\cdot}1v)},$$

is as near as any formula can be which does not take account of any other conditions than those embodied in its four terms. The same formula may be applied for calculating the temperature rise of the internal cylindrical surface of a stator, v being, as before, the peripheral velocity of the rotor in metres per second.

EXAMPLE 35. The internal cylindrical surface of the stator of a turbo-generator is 2960 sq. in., and the number of watts of heat flow communicated to the air by this surface is 11,700. If the peripheral velocity of the rotor is 92 metres per second, find the probable average rise of temperature of the surface of the stator above the average temperature of the air in the air-gap

$$\frac{11{,}700}{2960 \times 6{\cdot}45} = 0{\cdot}61 \text{ watt per sq. cm.}$$

$$t^\circ = \frac{0{\cdot}61 \times 333}{1+0{\cdot}1 \times 92} = 20^\circ \text{ C.}$$

An actual test, made on a turbo-generator running under these conditions, showed a temperature rise of 19° C.

EXAMPLE 36. The revolving field of an A.C. generator has a diameter of 154 cm. and a speed of 375 R.P.M. The axial length of the armature iron is 29 cm. What number of watts can be dissipated from the internal cylindrical surface of the armature for a rise in temperature 35° C. above the temperature of the air?

The peripheral speed is 30 metres per second. We have therefore

$$35 = \frac{333 \times \text{watts per sq. cm.}}{(1+0{\cdot}1 \times 30)}.$$

Watts per sq. cm. $= 0{\cdot}42$.

The total surface is 14,000 sq. cm.

Watts dissipated from surface $14{,}000 \times 0{\cdot}42 = 5900$ watts.

The cooling of field coils. In predetermining the temperature rise † of field coils we have two distinct problems: first, to determine the temperatures of the external and internal surfaces of the coil, and secondly, to find the difference between the temperature of the hottest point in the coil and temperature of the surface. In the first problem we have a certain number of watts to dissipate from a surface of a certain area, and we are concerned with the cooling conditions on that area. In the second problem we are concerned with the heat-conducting qualities of the coil itself, and the rate of production of heat per cubic inch, or per cubic centimetre.

Cooling of the surface of the coil. A surface may be cooled either by air blowing against it or by the conduction of heat to the body of the pole.

Some designers make their shunt coils to be entirely air cooled. They provide such large air ducts between the coils and the poles that all heat passes to the air.

* For various forms of similar formulae see the paper quoted above, *Journ. Inst. Elec. Engrs.*, vol. 48, p. 674.

† See "Heating of Coils in Machines with Long Cores," E. Arnold, *Elektrot. Zeitschr.*, 30, p. 172, 1909; "Distribution of Temperature in the Interior of Field-coils of Rectangular Section," Humburg, *Elek. und Maschinenbau*, 27, pp. 677, 696, 1909; "Heating of Magnet Coils," Williams, *Electrician*, 63, p. 706, 1909.

Other designers make the coils a tight fit on the poles, and rely upon conduction of a large portion of the heat generated through the insulation to the body of the pole, whence it passes to the frame or is dissipated from the pole face. These two cases require rather different treatment.

In considering the cooling of a surface by means of moving air, we see that any rules that we may have must necessarily be of very limited application, and when applied to coils of complicated shapes in proximity to various obstructions, they can only give us a very rough idea of the temperature that a surface will attain. Reliable data can only be obtained from experiments on similar coils run under similar conditions. Still a rough rule is better than no rule at all, even if it is only of service in indicating the direction along which we may improve the design. In the matter of air cooling, stationary coils and revolving field coils come under different rules. With stationary coils we are dependent for our cooling

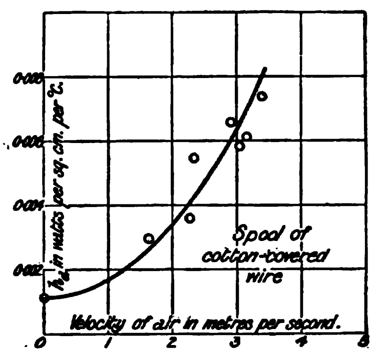


FIG. 232.—Relation between h_a the watts per square centimetre per ° C., and velocity of air when air blows upon a cylindrical coil.

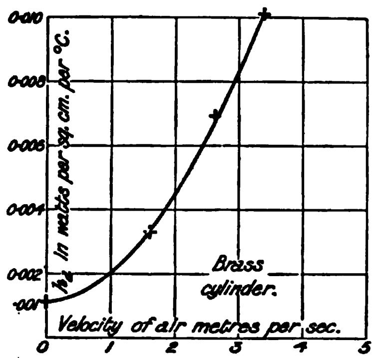


FIG. 233.—Relation between h_a the watts per square centimetre per ° C., and velocity of air when air blows upon a cylinder of tarnished brass.

upon the movement of the air, either by the fanning action of the armature or by some external agency, and the number of watts dissipated per sq. cm. will depend upon the velocity of the draught against the coils. For coils of approximately cylindrical shape, which present a surface of cotton-covered wire, the relation between h_a, the watts per sq. cm. per ° C. rise, and the velocity of the air impinging upon the side of the coil, is given by Fig. 232. The little circles give the results of a number of tests made on coils with the air blowing on both sides. The equation

$$h_a = 0{\cdot}0011\,(1 + 0{\cdot}54v^2)$$

gives approximately the law. We see that v comes into the equation in the second power, because, as we increase the velocity, not only do we increase the supply of air, but we increase the intimacy of contact between the air and the surface. In the case of draught of air blown in a direction parallel to the cooling surface, the cooling is proportional to the first power of v. Where the air is blown on only one side, the cooling effect is greatly dependent on the shape of the surrounding surfaces. We may take for the ordinary arrangement of field coils on a continuous current generator with the air blown from one side,

$$h_a = 0{\cdot}0011\,(1 + 0{\cdot}47v^2).$$

Where the air blows upon a bare metal surface, the cooling is much more effective. Fig. 233 shows the results of tests upon a tarnished brass cylinder with the air blown from both sides. The law is approximately

$$h_d = 0{\cdot}0011\,(1 + 0{\cdot}78v^2).$$

When there are ventilating ducts between the field coil and the pole, the cooling in the inside surface will be proportional to the velocity of the air in the duct. As it is impossible in most cases to find out what this velocity will be, the cooling constants can only be determined by experiments on coils of the same type running under similar conditions. The draught along these ducts is in most cases so low that the rate of cooling cannot be taken at more than 0·0012 watt per sq. cm. per ° C. rise of temperature. For this reason many designers prefer to do away with the duct between coil and pole, and cool the inside surface by conduction of heat into the pole. Even with a thickness of insulation (treated press-spahn) of 0·2 cm. and a liberal allowance for resistance of unavoidable air spaces, we can easily pass 0·003 watt per sq. cm. per ° C. difference of temperature. With thinner insulation and some care in eliminating the air space, we can pass as much as 0·007 watt per sq. cm. per degree.

Rotating field coils. The cooling conditions with rotating field coils are usually very much better than with stationary coils; nevertheless some care must be taken in the design to take full advantage of the circulation of air set up by the rotation. Ample space must be allowed for the air to get in between the coils and any obstructions to free circulation must be removed.

In rough calculations of the heat dissipated from the surface of revolving field coils, it is usual to take the total surface (both on the exterior and on the inside next to the pole) and to allow so many sq. ins. per watt. This method is good enough when we are comparing machines of the same general proportions and construction, and when we can get frequent check data from machines that have been tested. The method, though quick and handy in practice, does not help us to see how the cooling conditions will be altered when the design is modified.

In this rough method of calculation the following figures may be taken for coils of ordinary construction and well ventilated, with a speed about 5000 feet per minute, where the length of the poles is about equal to the pitch. For cotton-covered wire coils an allowance of from 1·2 to 1·4 square inches per watt will give about 40° C. rise. Where the coils are of bare copper strap, the allowance may be reduced to 0·8 to 1 sq. in. per watt.

It is better, where time permits, to take separately the cooling of (1) the ends of the coils exposed to the full draught, (2) the sides of the coils that lie parallel to the axis of the machine and (3) the internal surface lying next to the pole.

The ends of the coils. The area of the end will be taken to be the area obtained by multiplying the length e (Fig. 234) by the height of the coil, and in cases where the top and bottom of the ends are exposed to the air, their areas should be added. As a rule, the ends of the coils are flanked with fibre cheeks, which come against a support. Where this is the case, a short method, of sufficient accuracy, is to take the heat conducted through the cheeks as equal to the extra heat that would be conducted to the body of the pole, if the coil were a few centimetres longer than

it actually is. A rough guess can be made as to the number of centimetres to add to the length of the coil for this allowance as in the example given below.

If we denote by h_e the watts per sq. cm. per ° C. rise dissipated by the ends of a field coil revolving at a radius r_c centimetres, we find that the formula

$$h_e = 0{\cdot}0011\,(1 + 1{\cdot}2 \times R_{pm} \times \sqrt{R_{pm}} \times r_c \times 10^{-5}) \text{ watts per sq. cm.}$$

gives us values for the cooling constant which fit very well the results obtained from tests. In inch measure this becomes

$$h_e'' = 0{\cdot}007\,(1 + 3 \times R_{pm} \times \sqrt{R_{pm}} \times r_c'' \times 10^{-5}) \text{ watts per sq. in.,}$$

where r_c'' is measured in inches.

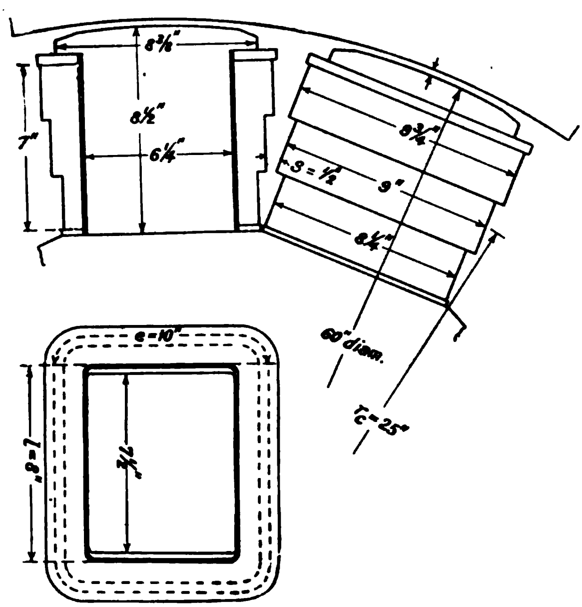


FIG. 234.—Dimensions of a coil upon which its cooling depends; 16-pole field-magnet.

The rate of **cooling of the sides of a coil** depends upon the ratio of the distance s to the length l (see Fig. 235). If we denote by h_l the watts per sq. cm. per ° C. rise dissipated by the sides of the coils, we find that the formula

$$h_l = 1{\cdot}5 \times 10^{-8} \times R_{pm} \times \sqrt{R_{pm}} \times r_c \times \sqrt{\frac{s}{l}} \text{ watts per sq. cm.}$$

gives good practical results. Here r_c is the radius in centimetres. This, in inch measure, becomes

$$h_l'' = 3{\cdot}8 \times 10^{-8} \times R_{pm} \times \sqrt{R_{pm}} \times r_c'' \times \sqrt{\frac{s}{l}} \text{ watts per sq. cm.}$$

The calculation of the **cooling of the internal surface** by conduction of heat to the pole is carried out as indicated on page 223.

The effect of lengthening a frame and of reducing the number of poles can be seen by the application of these rules to the cases given below.

In Examples 37 and 38 we have revolving field-magnets, each 60″ in diameter, one of 8 inches axial length and the other of 24 inches axial length. The mean radius of the coil is 25″. The clearance between the coils s is 0·5 inch. Thus, in one case $\sqrt{\frac{s}{l}}=0{\cdot}25$, and in the other it is 0·144. It will be seen that, notwithstanding the much larger cooling surface exposed in the longer machine, the total watts dissipated per coil are only 778, as against 516 in the case of the shorter machine.

Example 37. Fig. 234 gives the dimensions of a 16-pole field-magnet. The axial length of coil is 8″. The speed of the field-magnet is 375 R.P.M. Find the number of watts dissipated for 40° C. rise above the temperature of the air. Take the temperature of the pole at 8° C. above the air. We may take e, the length of the exposed end, at 10″.

$$\begin{aligned} h_e &= {\cdot}0011(1+1{\cdot}2\times R_{pm}\times\sqrt{R_{pm}}\times r_c\times10^{-6}) \\ &= {\cdot}0011(1+1{\cdot}2\times375\times\sqrt{375}\times63{\cdot}5\times10^{-6}) \\ &= {\cdot}0011(1+5{\cdot}5) \\ &= {\cdot}0011\times6{\cdot}5={\cdot}0071 \text{ watt per sq. cm. per } 1^\circ \text{ C. rise.} \end{aligned}$$

Area of ends of coils $=2\times7''\times10''\times2{\cdot}54^2$ cms. $=905$ sq. cms.

Watts dissipated at the ends, per coil $={\cdot}0071\times905\times40=256$ watts.

$$\begin{aligned} h &= 1{\cdot}5\times10^{-8}\times R_{pm}\times\sqrt{R_{pm}}\times r_c\times\sqrt{\frac{s}{l}} \\ &= 1{\cdot}5\times10^{-8}\times375\times\sqrt{375}\times63{\cdot}5\times\tfrac{1}{4} \\ &= {\cdot}00173 \text{ watt per sq. cm. per } 1^\circ \text{ C. rise.} \end{aligned}$$

Area of sides of coils $=2\times7''\times8''\times2{\cdot}54^2\times723$ sq. cms.

Watts dissipated at the sides, per coil $={\cdot}00173\times723\times40=50$ watts.

In calculating the heat conducted to the core we may add $1\frac{1}{2}$ inches to the length of the coil to allow for the heat conducted from the ends. As the insulation between coil and pole usually contains air spaces of uncertain dimensions, we may take the thickness as being 0·25 cm. and the heat conductivity at ·001 watt per sq. cm.

$$\text{Watts per sq. cm.}=\frac{32\times{\cdot}001}{{\cdot}25}={\cdot}128.$$

$$\begin{aligned} \text{Watts conducted to core, per coil} &= 30\times2{\cdot}54^2\times(7+1\tfrac{1}{2})\times{\cdot}128 \\ &= 210 \text{ watts.} \\ \text{Total watts per coil} &= 256+50+210 \\ &= 516 \text{ watts.} \\ \text{Total heat that can be dissipated from field coils} &= 16\times516 \\ &= 8{\cdot}3 \text{ K.W.} \end{aligned}$$

Example 38. Dimensions as in Fig. 234, but $l=24''$.

Watts dissipated from ends of coils (as above) $=256$ watts per coil.

$$\begin{aligned} h &= 1{\cdot}5\times10^{-8}\times375\sqrt{375}\times63{\cdot}5\times{\cdot}144 \\ &= {\cdot}001 \text{ watt per sq. cm. per } 1^\circ \text{ C. rise.} \end{aligned}$$

Area of sides of coil $=2\times24\times7\times2{\cdot}54^2=2170$ sq. cms.

Watts dissipated at the sides, per coil $={\cdot}001\times2170\times40=87$ watts.

$$\begin{aligned} \text{Heat conducted to core, watts per coil} &= 62\times(7+1\tfrac{1}{2})\times2{\cdot}54^2\times{\cdot}128 \\ &= 435 \text{ watts.} \\ \text{Total watts per coil} &= 256+87+435 \\ &= 778 \text{ watts.} \\ \text{Total heat that can be dissipated from field coils} &= 16\times778 \\ &= 12{\cdot}4 \text{ K.W.} \end{aligned}$$

EXAMPLE 39. Same diameter as before, but 8 poles instead of 16 (see Fig 235), speed 375 R.P.M.

$$r_c = 24'' = 61 \text{ cms.} \quad \sqrt{\frac{s}{l}} = \sqrt{\frac{4}{12}} = \cdot577.$$

$$\begin{aligned} h_e &= \cdot0011(1 + 1\cdot2 \times 375 \times \sqrt{375} \times 10^{-5} \times 61) \\ &= \cdot0011(1 + 5\cdot3) \\ &= \cdot0011 \times 6\cdot3 \\ &= \cdot00693 \text{ watt per sq. cm. per } 1^\circ \text{ C. rise.} \end{aligned}$$

Area of ends of coil $= 2 \times 18 \times 8 \times 2\cdot54^2 = 1860$ sq. cms.

Watts dissipated at the ends, per coil $= \cdot00693 \times 1860 \times 40 = 515$ watts.

$$\begin{aligned} h_t &= 1\cdot5 \times 10^{-5} \times 375 \times \sqrt{375} \times 61 \times \cdot577 \\ &= \cdot00383 \text{ watt per sq. cm. per } 1^\circ \text{ C. rise.} \end{aligned}$$

Area of sides of coil $= 2 \times 12 \times 8 \times 2\cdot54^2 = 1240$ sq. cms.

Watts dissipated at the sides, per coil $= \cdot00383 \times 1240 \times 40$
$= 190$ watts.

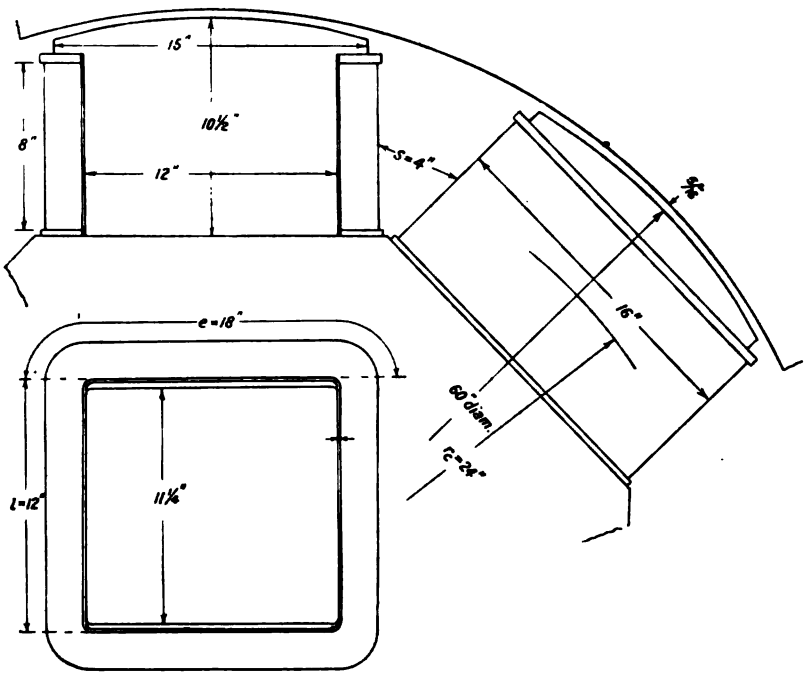


FIG. 235.—Dimensions of a coil upon which its cooling depends; 8-pole field-magnet.

In calculating the heat conducted to core, we may add 2 inches to the length of the core to allow for the heat conducted from the ends.

Watts per coil $= 50 \times 2\cdot54^2 \times (8+2) \times \cdot128$
$= 414$ watts.

Total watts per coil $= 515 + 190 + 414$
$= 1119$ watts.

Total heat that can be dissipated by field coils $= 8 \times 1119$
$= 8\cdot95$ K.W.

In these examples the temperature of the surface of the coils has been taken at 40° C. above the air. If this were the case the mean temperature of the coil would be a few degrees higher.

Having ascertained the approximate rise of temperature of the surface of the coil, the next step is to see how much higher the temperature of the inside of the coil is. In general, no calculation need be made of this, because the designer knows from experience of similar cases that the temperature is not too high. However, if a particularly deep coil is to be made, or one which contains an exceptionally large number of layers of fine wire, and in all cases where field coils are run at temperatures near the danger point, a calculation should be made of the rise of temperature of the interior of the coil over the surface.

The conduction of heat across the layers of insulated wires in a coil. The internal layers of a shunt coil, when heated up by the current, are hotter than the external layers. In a large number of measurements made by Mr. Rayner* on coils under running conditions, it was found that the temperature reached by the inside layers was frequently 50° C. higher than the temperature recorded

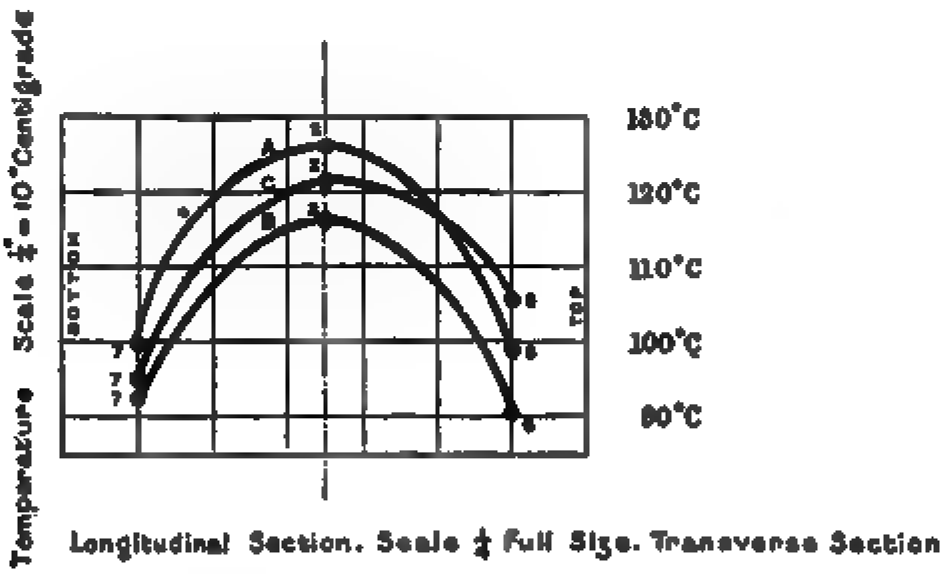


FIG. 236.

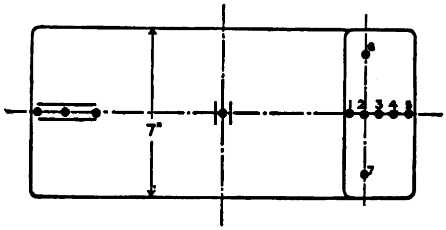
by a thermometer placed on the outside of the coil. In many cases the maximum temperature was 20° C. higher than the mean temperature measured by the resistance method.

As it is the maximum temperature reached that determines the life of the coil, it is important to design the coil so that this maximum temperature will not be too high. It is also important for the designer to know approximately how much higher the temperature of the coil will be in its hottest part than on the outside, so that he may (if he so desire) work the copper at its highest safe current density.

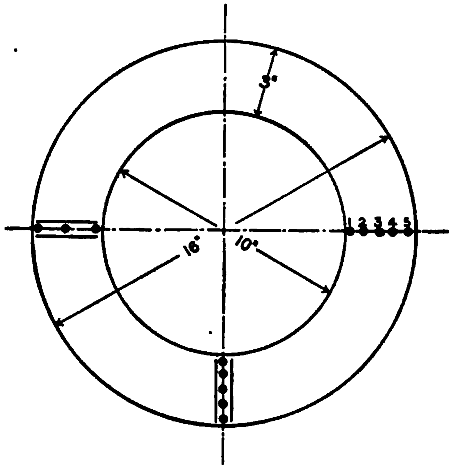
A general study of the distribution of temperature inside a coil shows that the hottest part is commonly midway between the top and bottom of the coil at a point a little way removed from the iron core. Fig. 236, taken from Mr. Rayner's paper, shows typical curves of temperature distribution inside a coil operating under practical conditions. The dimensions of the coil are given in Fig. 237. The temperatures were measured by thermo-couples inside the coil at the points indicated by the black dots in Fig. 237. This coil, one of six in a 94 H.P. motor, consisted of 2584 turns of wire 0·075" in diameter double-cotton covered. The whole coil was impregnated with insulating gum, and covered with tape. When curve A was obtained, the current density in the wire was 980 amperes per sq. in. The total

* *Jour. Inst. Elec. Engrs.*, vol. 34, p. 628.

watts converted into heat in the coil were 407. The machine was then running at full load at 325 R.P.M., the diameter of the armature being 31·1 inches. As the total surface of the coil (including the part next to the pole) was 816 sq. in., the watts per sq. inch were 0·5. It will be seen that the highest temperature reached was at a point about 1″ from the core, so that two-thirds of the heat travelled to the outside of the coil, and the other third (with the exception of some that came out at the ends) went towards the pole. In any coil the fraction of



ELEVATION



PLAN

FIG. 237.

the heat that goes towards the pole will depend upon the heat conductivity of the insulation between the coil and the pole and the temperature of the pole. In this case the rate at which heat was being conducted through the outer layers of wire at a point halfway between the top and bottom of the coil was about 0·525 watt per square inch. It will be seen from curve *A* that the temperature gradient at this point was 40° C. per inch.

Now this temperature gradient depends not only upon the watts per sq. in. of heat flux across the coil, but also upon the size and shape of the wire, the way that it is bedded and the nature of the insulation. If we are given full particulars of the size of wire, the thickness of the insulation, the space factor, the number

of turns and layers, the exciting current, and so on, we should be able to predetermine with a sufficient degree of accuracy the temperature of the hottest part of the coil.

The problem is somewhat analogous to the case already considered where the heat is conducted along copper conductors, but in this case the heat is conducted across one layer of conductors to another. The law of distribution of temperature takes the same general form:

$$T_x = T_{max} \cos p_1 x,$$

where T_{max} is the temperature of the hottest point measured from the artificial zero (240° below 0° C.), and T_x is the temperature of any point distant x centimetres from the hottest point along a line drawn in the direction of the flow of heat at right angles to the cooling surface. The value of $p_1 x$ in practice is such that $\cos p_1 x$ never assumes negative values.

If we examine the various curves given by Mr. Rayner * we will see that they are all part of cosine curves, except in those cases where there is a discontinuity in the coil.

Take, for instance, Test No. 2B. Add 240° C. to the ordinates of the transverse section curve on page 639 (vol. 34), and we obtain a curve like that given in Fig. 238. The law of this curve is approximately:

$$T_x = 356 \cos (0{\cdot}0975 x).$$

If the coefficient (p_1) of x is known,† and the distance from the hottest part is known, then we can calculate the amount that the temperature of the hottest part exceeds that of the surface. For instance, with the above law, if on the surface of the winding the temperature is 90° C. (330° above the artificial zero) and the hottest point is 4 cm. from the surface, then

$$330 = T_{max} \cos 0{\cdot}0975 \times 4,$$
$$T_{max} = 356.$$

The value of p_1 depends mainly on four factors:

(1) The current density in the copper.
(2) The thickness of the insulation per centimetre depth of coil and its nature.
(3) The space factor of the winding.
(4) The ratio of the length of the bobbin to the depth of the windings.

* *Journal of the Institution of Electrical Engineers*, vol. 34, p. 613. See also G. A. Lister, "The Heating Coefficient of Magnet Coils," *ibid.* vol. 38, p. 399.

† It is not possible to predetermine the value of p_1 in all the cases given by Mr. Rayner, because full particulars are not given of the thickness of the cotton coverings, but in several cases where we may assume the cotton covering is normal and the wires properly packed, the results agree closely with the values of p found by the author's experiments; for instance, in the case of coil No. 2 we have

$$p_1 = 127\sqrt{\frac{0{\cdot}63 \times 1{\cdot}6 \times 10^{-6} \times 0{\cdot}136}{0{\cdot}00095 \times 240}} = 0{\cdot}0975.$$

The value 127 amperes per square centimetre is obtained from the value 151 given in Mr. Rayner's table by the formula for I_x given above. Length of coil = 7 in., breadth = 3 in.; 7 + 3 = 10.

$$\sqrt{\frac{7}{10}} = 0{\cdot}84\,;\ 151 \times 0{\cdot}84 = 127.$$

In what follows we shall employ the following symbols:

l = length of bobbin in centimetres.

d = depth of winding in centimetres.

I_d = current density in amperes per square centimetre.

$I_x = \sqrt{\frac{l}{l+d}}\, I_d$.

σ = copper space factor.

i_n = thickness of insulation per centimetre of depth of winding.

k_h = heat conductivity of insulation in watts per square centimetre per °C. per centimetre of path.

Then $$p_1 = I_x\sqrt{\frac{1{\cdot}6 \times 10^{-6} \times \sigma \times i_n}{k_h \times 240}}.$$

In order to ascertain the values of k_h for round and for square wire, treated and untreated, experiments were made on the heat conductivity of cotton-covered wire windings. In Table XIV. are given values for k_h in some typical cases.

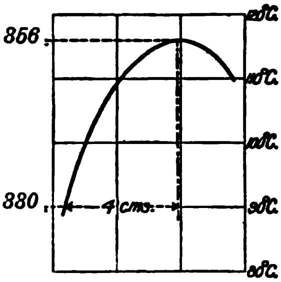


FIG. 238.—Curve showing distribution of temperature inside a shunt coil.

The figures given in this table allow a certain margin for variations in the construction of the coil which, so far as the tests went, appeared to be sufficient for tightly wound coils. For instance, the lowest value obtained for 0·032 in. round wire double-cotton covered and enamelled was 0·00065, and the highest value for 0·114 in. wire was 0·0009. For untreated wires both sizes averaged about 0·00055. It is possible that the margin given should be made wider. For loosely wound coils it will be very wide. The value of k_h is independent of the thickness of the insulation on the wire. The thickness of the insulation is taken into account in the formula in the quantity i_n, which is obtained by multiplying the number of layers per centimetre with the double thickness of cotton covering on each wire.

TABLE XIV. VALUE OF k_h FOR WIRE-WOUND COILS.

Kind of wire.	How treated.	Diameter of wire.	k_h
		Inches.	
Square wire double cotton covered	Made solid with heat-conducting enamel	0·114	0·00120 to 0·00140
Square wire double cotton covered	Untreated - - -	0·114	0·00090 to 0·00100
Round wire double cotton covered	Impregnated and made into solid block	0·03 to 0·114	0·00085 to 0·00095
Round wire double cotton covered	Treated with enamel - -	0·03 to 0·114	0·00065 to 0·00090
Round wire double cotton covered	Untreated, tightly wound -	0·07 to 0·114	0·00050 to 0·00060
Round wire double cotton covered	Untreated, tightly wound -	0·03 to 0·070	0·00040 to 0·00050
Round wire double cotton covered	Untreated, loosely wound -	0·03 to 0·070	0·00020 to 0·00035

EXAMPLE 40. A shunt coil of a c.c. generator is wound with 3480 turns of round double cotton-covered wire, dia. 0·080″ bare, 0·092″ insulated. The dimensions of the coil are as given in Fig. 239. There are 40 layers of 87 turns each. Between the coil and the pole there is a total thickness of $\frac{1}{10}$ inch treated fullerboard, and not more than $\frac{1}{32}$ inch of air space. There is a fan on the armature which creates a breeze, which is directed by the frame in an axial direction at a velocity of 2 metres per second against the sides of the coil. The ends of the shunt coil are flanked with $\frac{1}{8}$″ press-spahn, and are disposed in such a way that the cooling of the ends may be taken as about half as good as the sides.

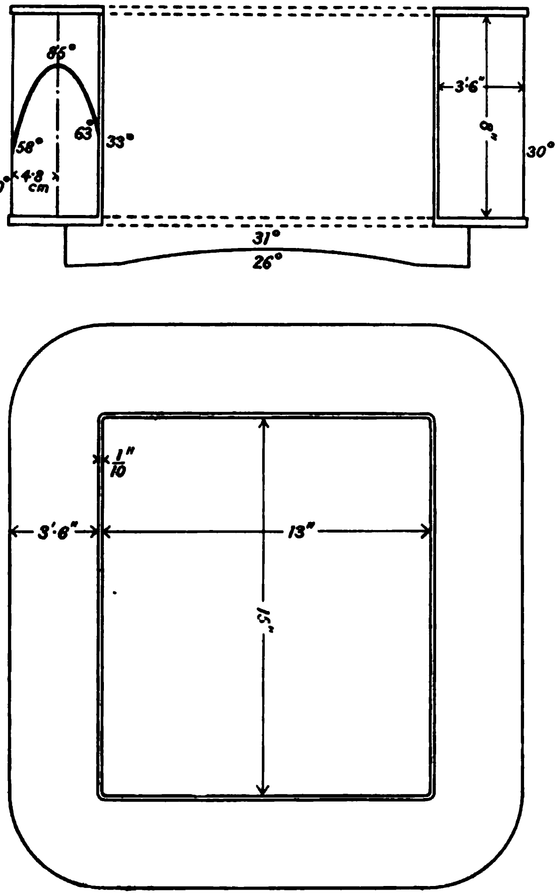


FIG. 239.—Dimensions of large shunt coil from which the temperature rise inside the coil can be approximately determined.

Find the maximum temperature rise inside the shunt coil after a long run at 4·35 amperes exciting current.

The total length of wire in the coil will be about 20,600 feet, having a resistance of 33 ohms cold or say 40 ohms hot. The total watts lost in the coil will therefore be about 760.

The thermal resistance of the fullerboard, 0·254 cm. thick, is 180 and of the air space 150, giving a total of 330; so that we have 0·003 watt conducted per sq. cm. per ° C. Now calculate the cooling coefficient of the external surface. This is

$$h_l = 0{\cdot}0011\,(1 + 0{\cdot}47 \times 2 \times 2)\,0{\cdot}0032.$$

If h_d for the ends is half this we may conveniently take half the area at 0·0032.

The cooling constants being approximately the same, we will as a first trial apportion the watts between the surfaces in proportion to their area. The area of the various surfaces are :

	Sq. cm.	Watts taken away.
Inside surface touching pole	2900	258
Outside surface	4000	357
One end surface	1620	145
	8520	760

Now find the temperature drop through the insulation with this provisional apportionment of the total watts :

$$\frac{258}{2900}=0{\cdot}089 \text{ watt per sq. cm.} \qquad \frac{0{\cdot}089}{0{\cdot}003}=\text{about } 30^\circ \text{ C.}$$

If the pole were 35° C. (10° hotter than the air), this would make the inside of the coil next to the insulation 65° C. Next find the drop of temperature between outside of coil and air :

$$\frac{357}{4000}=0{\cdot}089. \qquad \frac{0{\cdot}089}{0{\cdot}0032}=28^\circ \text{ C.}$$

If the air blown on the coil be taken at 30° C., this would give 58° C. for the running temperature of the exterior of the coil. Now see if this distribution of temperature will fit sufficiently well a temperature gradient curve with its apex in a suitable position to give the assumed flow of heat inwards and outwards. The copper space factor is 0·615. The total thickness of cotton covering per cm. is 0·135 cm. The value of k_h can be taken from Table XIV. to be 0·00095 The current density 134 amps. per sq. cm. must be multiplied by the coefficient

$$0{\cdot}83=\sqrt{\frac{8}{8+3{\cdot}6}}$$

to allow for the cooling towards the ends of the coils. Thus we have

$$P_1=0{\cdot}135\times0{\cdot}83\sqrt{\frac{0{\cdot}615\times1{\cdot}6\times10^{-6}\times0{\cdot}135}{0{\cdot}00095\times240}}=0{\cdot}0855.$$

The law of the temperature gradient of curve is

$$T_x=T_{\max}\cos(0{\cdot}0855\times x_1),$$

where x_1 is the distance of the apex of the curve from the outside surface. This distance x_1 must be found by trial and error. In fixing provisionally the position of the apex of the temperature gradient curve, we must remember that it is the watt-shed of the coil. It marks the position of the surface inside which all heat travels inwards, and outside which all heat travels outwards. The total volume of the coil should therefore be divided by the watt-shed plane into two volumes, one of which supplies the heat travelling to the inside and the other the heat travelling to the outside. If now, in our example, we put the watt-shed surface at a distance of 4·8 cms., from the outside, we will find that the amounts of heat generated in the volumes cut off are about in proportion to 357 and 258 respectively. We find $T_{\max}$ from

$$T_{x_1}=T_{\max}\cos(0{\cdot}0855\times4{\cdot}8),$$

where $T_{x_1}=(58^\circ+240^\circ)=298^\circ$. This gives us $T_{\max}=325^\circ$.

Thus the law of distribution of temperature within the coil becomes

$$T_x=(85+240)\cos(0{\cdot}0855x).$$

From this we find that the temperature of the copper next to the internal insulation works out to 63° C.

This is sufficiently near the assumed value 65 for us to accept the position taken for the apex of the curve. Fig. 239 then gives approximately the distribution of temperature under the prescribed conditions.

The passage of heat from the surface of ventilating ducts to the air flowing through. When air is blown through a ventilating duct, the distribution of the stream lines is usually very complicated. Generally we may say that the air in close proximity to the walls of the duct has a velocity much lower than the mean velocity, and the air in the centre of the duct has a velocity above the mean. In

what follows, when we speak of the velocity v of air in a duct, we mean the average velocity, that is to say, the number of cubic metres passing per second divided by the area of the cross-section of the duct in sq. metres. In turbo-generators, and other machines which are fed with a known quantity of air per second, the average velocity of the air in the ducts can be approximately calculated. But in open machines it can only be guessed at. Even if the guess is wide of the mark, it may be useful in comparing the performance of different machines, so long as we make the guess according to a definite rule. For instance, if we say that the velocity of the air in the ducts of the stator of an unenclosed generator or motor can be taken at one-tenth of the peripheral velocity of the rotor, we have a figure which, though very far wrong in some cases, nevertheless enables us to compare the cooling effects of ducts of the normal size in widely different machines in a more intelligent manner than if we allow the same specific cooling coefficient for all ducts, whatever the peripheral speed. The formula given below for the specific cooling of the walls of ventilating ducts is intended for use in turbo-generators and other machines in which the velocity in the ducts is approximately known. We may, however, use it in default of any other for calculating approximate figures for open-type machines.

From tests* on a turbo-generator, it was found that

$$h_v = K_v v.$$

Where h_v is the watts per square centimetre of cooling surface per °C., the difference of temperature between surface and air, v is the mean velocity of the air in the duct and metres per second, and K_v is a coefficient.

The value of the coefficient K_v was ·0014 in a number of tests in which v varied widely, and as the formula, applied in the way that we propose, was found to give the temperature rise with fair accuracy under practical working conditions, it is of more value than a formula based on laboratory experiments.

The coefficient 0·0014 is higher than would be obtained if the air were blown through a flat ventilating duct so steadily as to undergo no disturbance of the stream lines. Tests made under these conditions with a ventilating duct $\frac{1}{4}''$ wide gave a coefficient of 0·0005. The violent eddies which occur in air when it is discharged from the air-gap into the ventilating duct increase the cooling properties.

A round ventilating hole 2″ diameter, through which the air passes with steady stream lines, will give us $h_v = {}$·00033v, but if baffles are added to stir up the air the formula may be $h_v = {}$·0011v.

In applying this formula, v is taken as an average figure for the whole of the ducts. In finding the area of the cooling surface, the area of both walls must be counted.

Using the formula in this way, one will, of course, only get the average rise in temperature of the surface of the iron over the average temperature of the air in the ducts. Unless care is taken to distribute the cool air evenly, points may be found where the temperature rise is considerably above the mean. A good idea of the way in which the heat distributes itself in a turbo-generator, and of

* *Journ. Inst. Elec. Engineers*, vol. 48, p. 703.

the factors which control the temperature rise, is obtained from the following description of experiments on an 1875 K.V.A. generator running at 3000 R.P.M. The machine was totally enclosed and ventilated by means of a fan at each end, in the manner shown in Fig. 215.

In certain parts of the machine ordinarily inaccessible to thermometers, thermo-couples were placed while the machine was in course of construction. Thus, in the centre of the packet of punchings lying between the ventilating ducts Nos. 5 and 6 (see Fig. 240), thermo-couples were placed at the points M, N, O and P. Then, in the ventilating ducts at the lower part of the machine, couples were exposed to the full blast of air, so that readings could be taken of the temperature of the air in the ducts at that part to compare with the temperature of the air in the same ducts at the top of the machine.

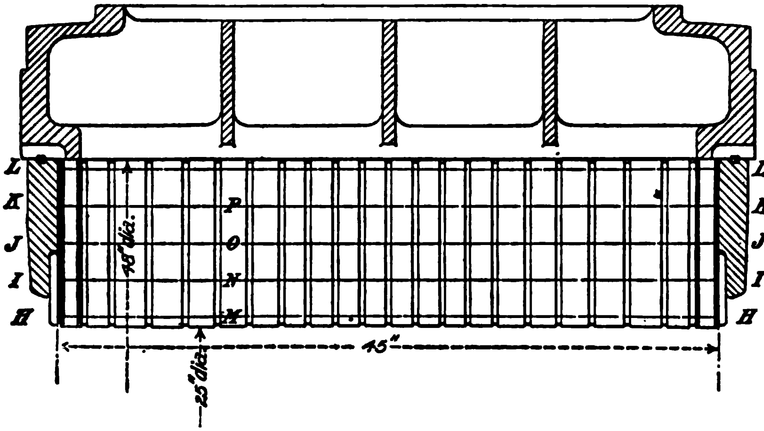


FIG. 240.—Showing depths H, I, J, K and L, to which thermo-couples were inserted into vent ducts.

The generator was run at no load, and the iron loss, friction and windage measured in the ordinary way by measuring the power supplied to the driving motor. The rotor was mounted in its own bearings, and coupled to a driving pulley mounted on independent bearings. The pulley was driven by a direct-current motor at 3000 revs. per minute. The power taken to drive the pulley alone with the full tension on the belt was found to be 13 K.W. The sum of the friction of the generator bearings and the windage was found by deducting 13 K.W. from the whole combination. With full aperture allowed to the fan, the sum of the friction and windage amounted to 46 K.W. In order to ascertain the amount of power lost in the bearings, measurements were made of the quantity of oil supplied to each bearing per minute and the rise in temperature of the oil. A rough estimate was also made of the quantity of heat lost by the bearings by radiation and convection. It was found that the heat carried away by the oil in one bearing was equivalent to 7·9 K.W., and from the other 6·7 K.W. The radiation and convection losses of the two bearings together was less than 1 K.W., so the total bearing losses were about 15·6 K.W. This left 46 – 15·6 = 30·4 K.W. for the windage with full aperture, giving 8800 cub. ft. of air per minute at 50° C. With a reduced aperture giving 4400 cub. ft. of air per minute, the windage loss was 22·8.

The amount of air passed through the machine per minute was measured in two different ways: (1) An anemometer was used to find the mean velocity of air at the exit in feet per minute, and this multiplied by the area of the exit in square feet gave roughly the cubic feet per minute. (2) The total rise in temperature of the air in passing through the machine was measured, and from the known losses causing the heating, the flow of air could be calculated. The first method was not as accurate as the second. It gave on the average an air velocity from 5 to 7 per cent. too high. We have therefore adopted the figures given by the second method. These are probably right within 5 per cent. It must be remembered that what we are really concerned with is the weight of air passed through the machine per minute. The volume of the air changes with the temperature quite appreciably. Thus, at 20° C., 750 lbs. of air have a volume of 10,000 cub. ft., while at 60° C. the volume is 11,400 cub. ft. There were three tests, which we distinguish by the letters *A*, *B* and *C*.

In test *A* the air supply was cut down to about half its normal flow. The field-magnet was excited with 133 amperes (about 30 per cent. more than the no-load field current). The resulting iron loss was 43·5 K.W., and the I^2R loss in the field-magnet was 8·5 K.W. Thus the total losses going to warm up the air were:

	Kilowatts.
Windage	22·8
Excitation	8·5
Iron loss	43·5
Total	74·8

After running 4 hours, the temperature of all the parts of the machine rose within half degree of their final temperature. The air entered the machine at an average temperature of 21·7° C., and was expelled at an average temperature of 53·2° C., giving a temperature rise of 31·5°. The heated air did not represent the whole of the heat produced. The cast-iron frame presented a cooling surface of 10,900 sq. in., and had a mean temperature over the air of 28° C., so that it would radiate * in almost still air about:

$$10{,}900 \times 0{\cdot}008 \times 28 = 2{\cdot}44 \text{ K.W.}$$

The cast-iron blocks upon which the frame rested would carry away not more than 1·5 K.W. Let us say that 4 K.W. was lost by the frame. This is such a small fraction of the whole that we need not estimate it very accurately. Then we have 74·8 − 4 = 70·8 K.W. carried away by the air. Now 1 K.W. is equal to 240 calories per second, so we have

$$70{\cdot}8 \times 240 = 17{,}000 \text{ calories per second.}$$

* It is of interest to note that a small fraction of the total losses on a large turbo-generator are dissipated by external radiation; in this case about 5½ per cent. In the case of a medium-speed generator of 5 K.W. about 50 per cent. of the losses can be accounted for in external radiation. Radiation is used here in its commonly accepted inaccurate sense, and includes convection to nearly still air. The true radiation by heat waves is rather less than half of these figures, and may be calculated approximately from the formula:

H in gram calories per sec. = surface in sq. cm. of equivalent sphere $\times \sigma \times (T_1^4 - T_2^4)$, where $\sigma = 0{\cdot}6 \times 10^{-12}$ for a dark painted generator and T_1 = temperature of generator in °C. above absolute zero. T_2 = temperature of surrounding objects above absolute zero.

Taking the specific heat of air at 0·2375, we have

$$\frac{17{,}000}{0{\cdot}2375} \times \frac{1}{31{\cdot}5} \times \frac{1}{453} \times \frac{60}{1} = 300 \text{ lbs. of air per minute,}$$

or 4400 cub. ft. of air at 53° C. The anemometer measured on the average 4800 cub. ft. of air per minute. This reading must be too high, because 4800 cub. ft. of air per minute raised in temperature 31·5° represents more power than was actually supplied to the machine, so we will take 4400 cub. ft. as about right.

It is interesting now to see exactly how the air was heated up as it passed through the machine.

The temperature of the air in the various ventilating ducts and in the air-gap was measured by a pair of thermo-couples, mounted on a long wooden rod, which could be moved about in the ducts while the machine was running. Two couples of equal resistance connected in parallel were used, one on each side of the rod, so that if there were any difference between the temperature of the air on one side of the duct and the other, the reading obtained gave the average value. The couples were of such a very thin wire (0·01 in. diameter), and were mounted in such a way that, when exposed to a breeze, they assumed the temperature of the air almost immediately. It was therefore possible to take very rapidly a large number of readings of the temperatures at different depths in each air-duct, and to plot curves such as those given in Fig. 241. The lines marked *H*, *I*, *J*, *K* and *L* are drawn through the points which give the readings of temperature rise at different depths in the ventilating ducts as indicated by the dotted lines in Fig. 240 bearing the corresponding letters. The hole at the top of the frame at which the air was expelled measured 36 × 20 in., and it was only over this area that it was possible to insert the wooden rod carrying the thermo-couples. In some parts within reach of this hole a flexible strip of press-spahn with a thermo-couple attached was used to take check readings, and the couples placed in the ducts in the lower part of the machine (that is to say, below the rotor) were used as a further check. These lower couples gave readings 2° or 3° higher than couples placed in the same ducts in corresponding positions at the top of the machine. This was possibly on account of the slightly lower velocity of the air thrown downwards, there being a certain amount of back pressure produced by the resistance of the flow of air through the annular space in the frame. As far as could be ascertained by a number of check readings taken over the field available from the exit hole at the top, the chart in Fig. 241 represents fairly well the distribution of temperature in the ducts in the top half of the machine, and if similar charts had been taken in radial planes at various angles all round the machine, the chart would have been very similar, but all the temperatures would have been gradually raised about 3° as we approached the planes lying below the rotor.

Temperatures were at the same time taken of the air admitted, of the air in the end bells, in the gap, in the yoke, and at eight different points distributed over the exit.

The average temperature of the air drawn into the machine was 21·7° C. It will be convenient to speak of the temperature rise over this initial figure, rather than of the actual temperature of the air. In the end bell at the points *F* and *G*

the temperature had risen 9·8° C. and 10·2° C. respectively. This rise was due partly to the work done to the air due to the centrifugal blowers, partly windage and I^2R losses from the end bells of the rotor, and partly from heat radiated from

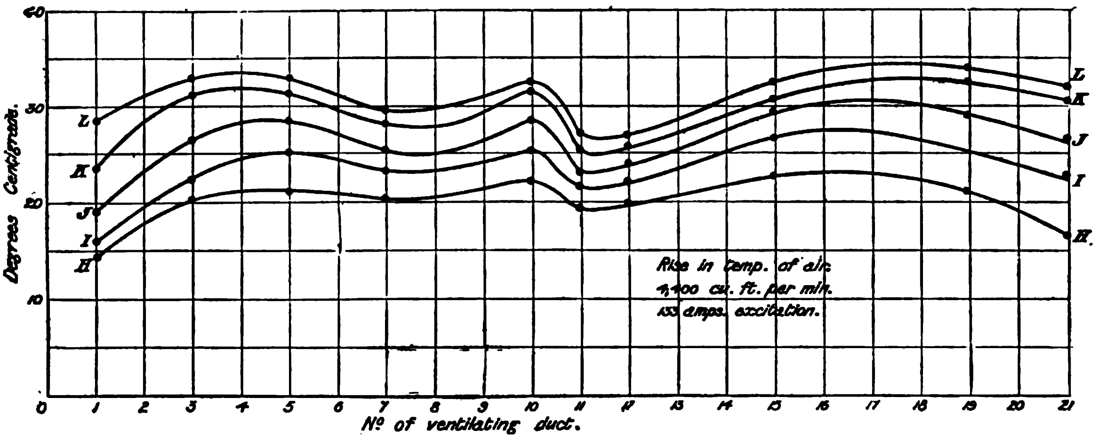


FIG. 241.—Temperature rise of air at different depths in the ventilating ducts. Iron loss, 43·5 K.W. Air supply, 4400 cub. ft. per minute.

end plates of the machine. The lowest temperature recorded in the air-gap was at the entrance to the first ventilating duct. Here the rise was 14° C. As we passed from the first vent duct to the centre of the machine the temperature was

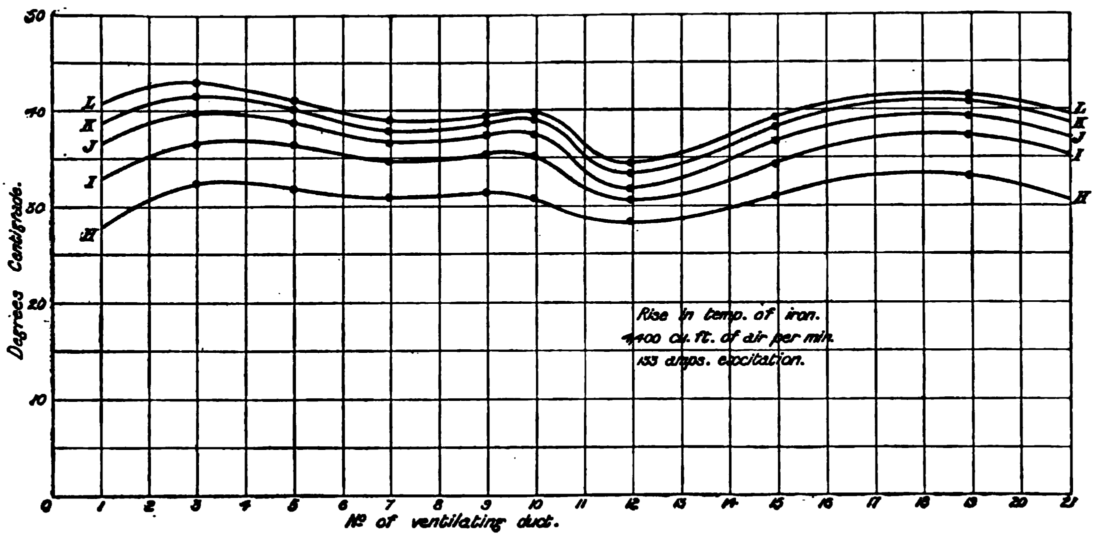


FIG. 242.—Temperature rise of surface of iron in ventilating ducts.

higher, but the increase followed an irregular law, as indicated by the wavy line marked position H in Fig. 241. The curious dip in the curve in the centre of the machine, which is also seen in the curves of temperature rise in the iron in Fig. 242, only occurred when the air supply was throttled. It does not occur in Figs. 245,

246, and 249. The throttling of the air supply reduced the pressure in the end bells from 4·25 in. of water for full aperture to 0·75 in. of water. Thus the blast along the air-gap must have been very much reduced while the blowing action of the vent ducts on the rotor would have taken a more important part in the scheme of ventilation than when the full blast was in operation. Owing to the meeting of the two opposing currents of air in the axial holes in the rotor, there is a tendency for the pressure of air from the rotor to be greatest in the middle, and this increased pressure probably gave a supply of rather cooler air near the centre of the machine.

The velocity of air at the exits of the different vent ducts, though not perfectly constant as one passed from duct to duct, was only very slightly greater in the centre of the machine than at the ends. It is therefore sufficient for our purpose to take the mean temperature rise of the air entering the ducts as derived from curve H at 20·5° C.

Let us now calculate the number of kilowatts, x, required to heat up the air to 20·5° C. We have, from our previous calculations,

$$\frac{x}{70{\cdot}8}=\frac{20{\cdot}5}{31{\cdot}5},$$
$$x=46 \text{ K.W.}$$

Now the windage only amounted to 22·8 K.W. and the I^2R in the field to 8·5, so that we have 14·7 K.W. in addition which must have been supplied by the iron loss, and communicated to the air mainly on the cylindrical face of the armature. A small amount—probably about 3 K.W.*—would be supplied to the air from the end plates of the armature. Deducting this, we have about 11·7 K.W. conveyed to the air by the cylindrical face of the armature. As we have seen above, we are able from this data to calculate the specific rate of cooling per square inch of armature face.

As the air passes along the vent ducts, the temperature rises; in some ducts the air received as much as 11·5° further rise in temperature, in others not more than 8° rise, the mean being about 10·2° rise. If y is the power expended in heating up the air 10·2°, we have

$$\frac{y}{70{\cdot}8}=\frac{10{\cdot}2}{31{\cdot}5},$$
$$y=23 \text{ K.W.}$$

Now the air passes into the annular space in the frame and picks up a little more heat from the punchings. Part of this extra heat is communicated to the frame and is radiated from the outside, and part goes to raise the temperature to 53·2°, giving a total temperature rise of 31·5°.

The next point of interest is the distribution of temperature in the iron punchings. The thermo-couples were placed in the centre of a packet of punchings at the points M, N, O, P (see Fig. 240), another couple was placed at Q, just behind the first punching in the packet; the packet in question was the one between ducts 5 and 6 in Fig. 240. For the purpose of taking rapid readings of the tempera-

* That this amount is small can easily be seen when we come to calculate the amount of heat given to the air in one ventilating duct.

ture on the surface of the iron punchings within the ducts, an instrument was made, which consisted of a piece of copper foil 0·125 × 0·75 × 0·01 in. soldered to a thermo-couple mounted on a velvet cushion, and arranged on a wooden rod, so that it could be pushed down the ventilating ducts and pressed against the sheet iron. A spring was provided at the back of the cushion to give the requisite pressure, the copper foil being shielded from draughts by the cushion, and being of small heat capacity very soon assumed the temperature of the iron against which it was pressed; thus one could read off directly on a millivoltmeter the temperature of any surface against which the copper foil was placed.

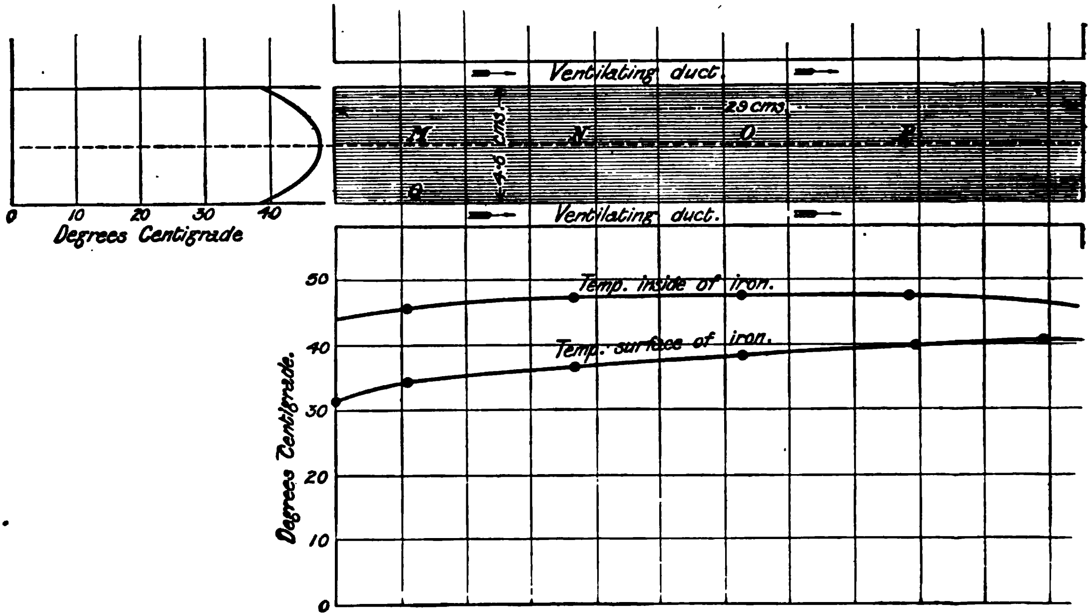


FIG. 243.—Temperature rise inside one packet of iron punchings 4·6 cm. thick and 29 cm. deep. Loss about 0·055 watt per cubic centimetre.

Fig. 242 gives the distribution of temperature of the iron on the surface of the various ducts in test (A). The curves *H*, *I*, *J*, *K* and *L* correspond with the positions in the ducts shown by the lines in Fig. 240.

It will be seen that these curves follow the general shape of the curves giving the rise of temperature of the air, but they are, on the whole, about 10·5° higher for the position *H* and 8·5° for the position *L*.

If we take the average value of the temperature of the air at the position *H*, then the average value at the position *I*, and so on, and plot these average values, we get a curve giving the mean temperature rise of the air as it passes through the ventilating ducts, like that shown in Fig. 244. The ordinates in this figure give the rise above the temperature at which the air enters the machine, the rise before reaching the ducts being 20·5°, and the rise in the ducts being 10·2° C.

Taking similarly average values of the temperature of the iron at the various positions, we get a curve of temperature rise of the surface of the iron.

As the total amount of air passing through the ventilating ducts was 300 lbs. per minute, it is possible to calculate the watts absorbed by the air from the rise in temperature by the formula:

$$\text{Kilowatts} = \frac{300 \times 453 \times 0{\cdot}2375 \times \text{temperature rise}}{60 \times 240}.$$

Plotting the kilowatts absorbed by the air, we get the curve shown in Fig. 244. The velocity of the air in that part of the ventilating duct which was narrowed by the armature coils was 8·4 metres per second, in the part of the ventilating duct beyond the armature coils the velocity was 4·5 metres per second, and at the exit of the ventilating ducts the velocity fell to 3·2 metres per second. Plotting these figures, we get the velocity curve in Fig. 244. We have given, on the same figure,

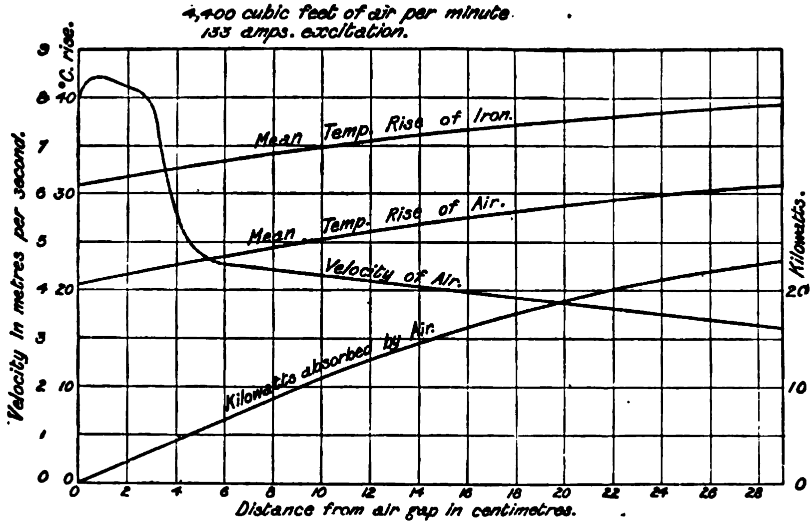


FIG. 244.—Curve showing how the air is raised in temperature as it passes along the ducts and the number of kilowatts absorbed.

the difference of temperature between the iron surface and the air, the velocity of the air and the watts absorbed. If we take the slope of the curve giving the watts absorbed, say at a point 16 cms. from the entrance to the duct, the slope of this line gives us the kilowatts absorbed by the air per centimetre travel. At the point of the 16 cms. the rate is 800 watts per centimetre. The temperature difference between the air and the iron at this point is 9°, and the total area of the ventilating ducts to which the air is exposed in traversing the centimetre length of path is:

$$(300 \times 2 \times 21) + (72 \times 2 \times 21) = 15{,}600 \text{ sq. cms.}$$

If we denote by h_v the watts per square centimetre of cooling surface per °C. difference of temperature between surface and air, we have

$$h_v = \frac{800}{9 \times 15{,}600} = 0{\cdot}0057.$$

This is at an air velocity of 3·95 metres per second.

In order to see the effect on the distribution of temperature throughout the machine with a greater draught, in test (B) the air supply was increased to 8800 cub. ft. per minute, the iron loss and excitation losses being as before. The temperature of the air in various ducts is given in Fig. 245, and the temperature of

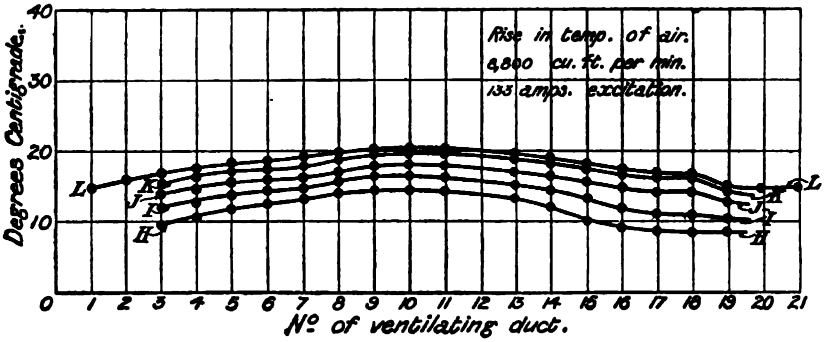


FIG. 245.—Rise in temperature of air with air supply doubled and losses as before. Test (B).

the various parts of the surface of the ducts in Fig. 246. Plotting the average values of the air temperatures at different depths in the ducts, we get the curve marked "Temperature rise of air" (Fig. 247), and plotting the average values of the surface temperatures of the iron, we get the curve marked "Temp. rise of iron" (Fig. 247). On this Fig. is also plotted the velocity of the air as it passes along the ducts and the kilowatts absorbed by the air. Taking the tangent of the watts

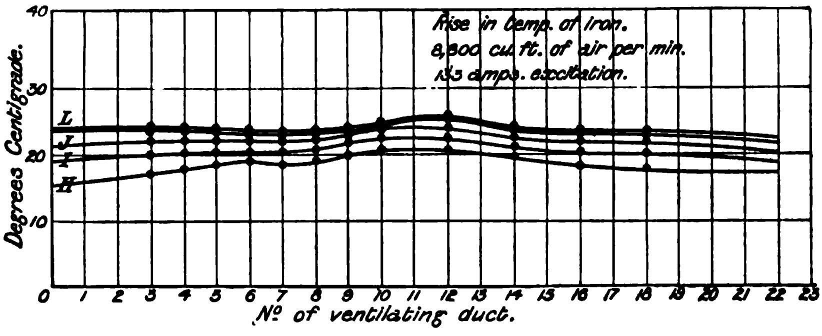


FIG. 246.—Rise in temperature of iron with air supply doubled and losses as before. Test (B).

absorbed at the point 16 cms. from the internal cylindrical face of the stator, we find that the air is picking up heat at the rate of 1250 watts per centimetre length of path. The difference of temperature between iron and air at this point is 7° C., and the total area of surface exposed for 1 cm. of path is 15,600 sq. cms. as before; we therefore have

$$h_v = \frac{1250}{7 \times 15{,}600} = 0{\cdot}01146,$$

the velocity of the air being 7·9 metres per second. We see from these experiments, therefore, that h_v (the watts per square centimetre of cooling surface per ° C.

difference of temperature between surface and air) is almost exactly proportional to the velocity of the air. h_v, in fact, is given by the equation :

$$h_v = 0{\cdot}00145v,$$

where v is the velocity of the air in the ventilation duct in metres per second (see Fig. 248).

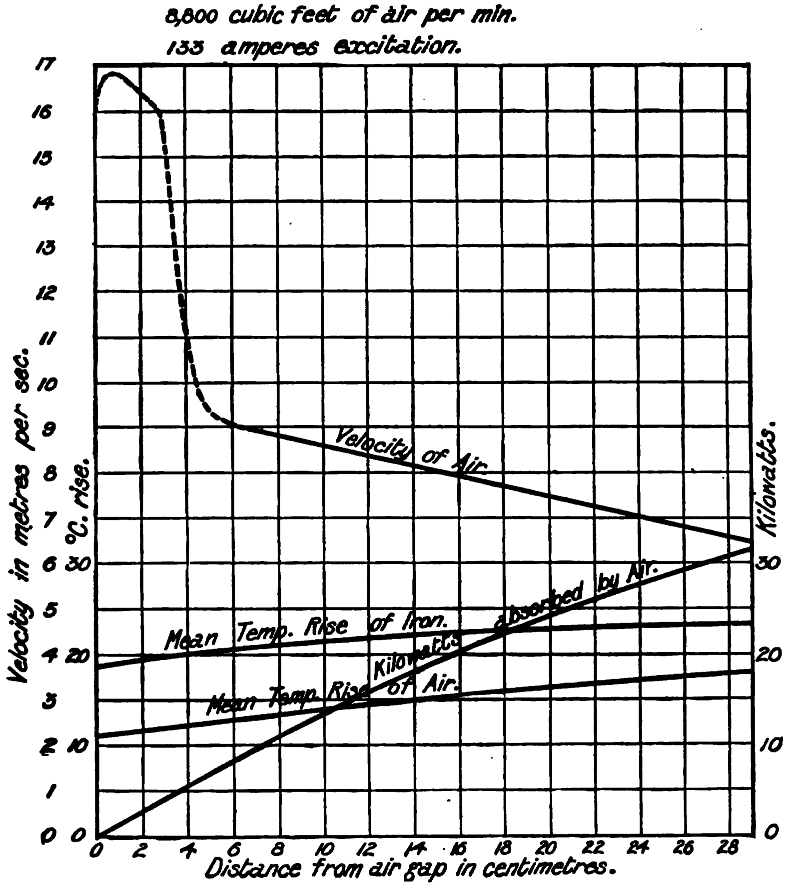


FIG. 247.

In test (C) the air supply was maintained at 8800 ft. per minute, but the iron loss was increased to 56 K.W. and the excitation losses to 17·5 K.W. Under these conditions the temperature distribution of the air and iron in the ducts is given by Figs. 249 and 250 respectively.

Conductivity of iron punchings. If we have a packet of iron punchings in which the loss per cubic centimetre is constant, and if all the heat generated is conducted across the packet and given off symmetrically to the air in the ventilating ducts which bound it on each side, the hottest part of the punchings will be in the centre, and the temperature gradient at any point within the iron will be

proportional to x, the distance of the point from the centre. Let w be the watts lost per cubic centimetre, then $w\,dx$ will be the loss in a little part of the iron

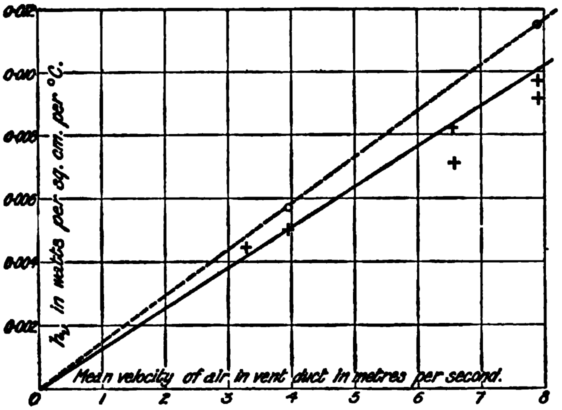


FIG. 248.—Relation between h_v, the watts per square centimetre per ° C. (difference in temperature between iron and air), and the velocity of air in the ventilating duct.

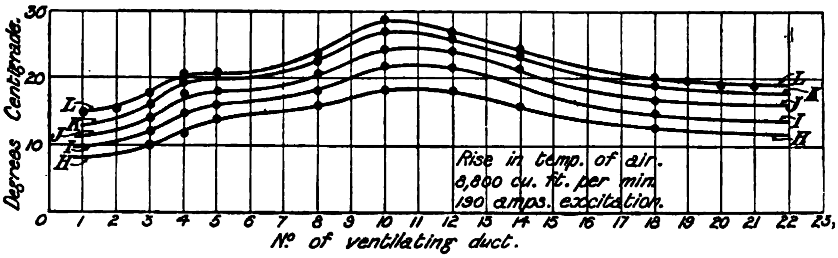


FIG. 249.—Rise in temperature of air with supply doubled and iron loss increased to 56 K.W. Test (O).

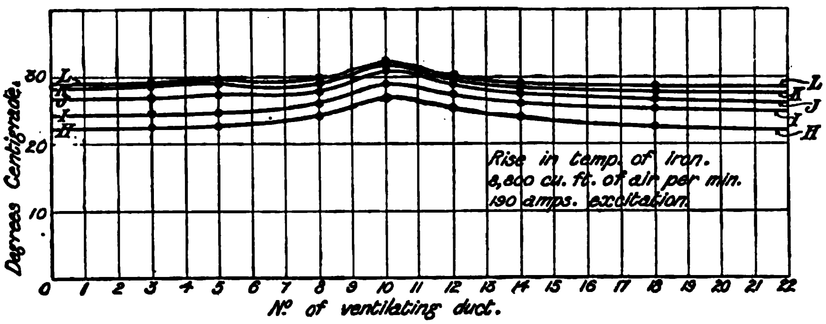


FIG. 250.—Rise in temperature of iron with air supply doubled and iron loss increased to 56 K.W. Test (O).

laminations 1 sq. cm. in area and dx cms. thick. The total heat generated in a block 1 cm. high and 1 cm. wide, and of length x will be wx. If K_h is the heat conductivity in watts per square centimetre per ° C. difference of temperature

per centimetre, the temperature gradient $\frac{d\theta}{dx}$ multiplied by the heat conductivity is equal to wx. As $\frac{d\theta}{dx}$ is negative when x is positive, we have

$$-K_h\frac{d\theta}{dx}=wx,$$

$$\theta=\text{constant}-\frac{w}{2K_h}x^2.$$

The curve of temperature distribution within the iron is therefore a parabola such as that plotted in Fig. 243.

In the experiments above described, measurements were made of the temperature in the centre of the packet and on the exterior. Knowing the loss per cubic centimetre of iron, we can calculate K_h as follows:

EXAMPLE 41. The total iron loss amounted to 43·5 K.W. Of this 4·5 K.W. was in the teeth, and 39 K.W. behind the teeth. The total volume of iron behind the teeth was 710,000 cub. cms.

Thus, $\frac{39,000}{710,000}=0·055$ watt per cubic centimetre $=w$.

It is seen from Fig. 243 that the temperature on the medial line between P and N was almost constant for points on both sides of O, so that the amount of heat conducted from O towards P and N would not be very great. It would not be negligible, because the conductivity of the punchings in this direction is so much greater than the conductivity across the laminations. Let us take the figures at the point O, where the heat conducted along the laminations is at a minimum, and calculate the conductivity on the assumption that all the heat flows to the walls of the ventilating ducts.

Now there are 8° difference of temperature between the centre and the surface of the packet, so we have

$$38=46-\frac{0·055}{2K_h}x^2 \text{ (see Fig. 243).}$$

$$8=\frac{0·055}{2K_h}(2·25)^2.$$

$K_h=0·0174$ watt per square centimetre per °C. per centimetre.

$K_k=0·0042$ calorie per second per square centimetre per °C. per centimetre.

The formula given * by Dr. Ott for the heat conductivity across laminations is

$$K_k=\frac{\delta_1+\delta_2}{\frac{\delta_1}{K_1}+\frac{\delta_2}{K_2}+\frac{1}{a}},$$

where

δ_1 = thickness of iron in centimetres.

δ_2 = thickness of insulation in centimetres.

K_1 = conductivity of iron = 0·15.

K_2 = conductivity of insulation (paper = 0·0003) (varnish = 0·0006).

a = conductivity of rough surface. This may be between 0·5 for smooth and 0·04 for very rough iron.

In our experiments $\delta_1=0·041$, $\delta_2=0·0033$, the insulation being paper. The formula gives $K_k=0·0035$.

* Ludwig Ott, *Mitt. u. Forschungsarbeiten*, Heft 35 and 36, p. 53. See also T. M. Barlow, "Heat Conductivity of Iron Stampings," *Journal of the Institution of Electrical Engineers*, vol. 40, p. 601; R. P. Gifford, "Influence of Various Cooling Media upon the Rise of Temperature of Soft Iron Punchings," *ibid.* vol. 44, p. 753 (1910

In the experiments described the loss per cubic centimetre, 0·055 watt, was rather high. This was because the machine was run at 30 per cent. above its normal field excitation. A more usual figure for 50 cycles would be 0·045 watt per cubic centimetre. If then we take the conductivity of the punchings at 0·0174 watt per square centimetre per ° C. per centimetre, then we have θ, the difference in temperature between the surface and middle of the packet

$$=\frac{0{\cdot}045}{2\times 0{\cdot}0174}x^2.$$

For a packet 4·5 cms. thick ($x=2{\cdot}25$) the excess of temperature would be 6·5° C., and the mean temperature of the iron above the surface only 4·5° C.

At 25 cycles the loss per cubic centimetre would be about 0·025 watt per cubic centimetre. Here the packets might be about 6 cms. thick for the same temperature rise in the hottest part.

In any case, it is seen that, unless the packets are made much thicker than is usual in practice, the temperature rise in the centre due to the poor heat conductivity across the laminations is not of very great importance.

Cooling of external surface of stators. The cooling of the iron of a stator is considerably helped by the conduction of the heat into the cast-iron frame, from which it passes by radiation and convection to the surrounding air. On slow-speed machines on which the depth of punchings is usually small compared with the depth of the frame, this cooling by conduction is of more importance than on turbo-generators with very deep punchings. It is in general impossible to make an accurate calculation of the amount of this conduction, and yet one must make an allowance for it in machines with shallow iron. Perhaps the simplest rule, and one which gives a result not very far from the truth, in machines of normal construction, is to allow 0·15 watt per sq. cm. for the whole of the external surface of the punchings. This allows a temperature rise of 40° C. above the air. By external surface we mean the external circumference of the punchings multiplied by the gross length plus the area of the end plates flanking the iron at both ends.

EXAMPLE 42. In the 750 K.V.A. generator illustrated in Fig. 332 we have a bore of frame of 184 cms. and a length of 31·8 cms. So the area of the bore is 18,000 sq. cms. The total area of the flanks of the iron on both ends is 15,000 sq. cms., giving altogether 33,000 sq. cms. Multiply by 0·15 watt per sq. cm., and we get 4950 watts conducted and radiated from the outside area.

Collection of rules for predetermining the cooling conditions. We may then collect our rules for ensuring the cool running of a machine as follows:

1. Sufficient air must be provided to carry away the heat generated. A supply of 100 cub. ft. of air per minute will in general be sufficient (see pages 206, 216 and 245).

2. Sufficient cooling surface must be provided to communicate the heat to the air, and the short rules given below will in general tell us how much surface to provide.

3. For ventilating ducts we may take the formula

$$h_v=K_v v,$$

where h_v is the watts per sq. cm. of cooling surface per ° C., and K_v is a coefficient

between ·0003 and ·0014 (see p. 242). The difference in temperature between surface and air and v is the mean velocity of the air in the duct in metres per second. Where the machine is enclosed and provided with a definite amount of air v is known. In other cases it should be roughly estimated from the circumstances. A rule which works well enough in practice is to take v in the ducts at one-tenth the peripheral velocity of the machine (see page 325).

4. For the cooling of the surface of rotors and the internal cylindrical face of stators we may take the formula

$$\frac{\text{watts per sq. cm.}}{{}^\circ\text{C. rise}}=\frac{1+0{\cdot}1\,v}{333}, \qquad (1)$$

where v is the peripheral velocity of the machine in metres per second.

5. To find the number of watts conducted to and dissipated by the external frame for a temperature rise of 40° C., multiply the "external surface" (see page 325) by 0·15 watt per sq. cm.

6. To find the difference of temperature between an armature coil and the surrounding iron, one can adopt the method given on page 224, using the constants for the heat conductivity of the insulating material given in Table XIII., and allowing for air-spaces whose resistance is given roughly by Fig. 226.

7. To find the temperature rise of the surface of wire-wound coils upon which the air is blowing with a velocity of v metres per second, we may take the formula

$$h_d=0{\cdot}0011(1+0{\cdot}54v^2). \qquad (2)$$

8. To find the difference between the inside temperature of a wire-wound coil and the external temperature, we may follow the method given on page 236.

9. To find the difference between the temperature of the centre and the cooler parts of a hot-bed of conductors cooled mainly by the conduction of the heat along the conductors, we must adopt the method given on page 227.

10. To find the watts dissipated by the surfaces of a revolving field-coil, we may adopt the rules laid down on pages 232 to 235.

Examples of the application of these rules in actual cases will be found on pages 323, 349, 389, 454, 492 and 545.

The articles * referred to below bear upon the subject under consideration, and will be of service to the reader.

* "Heating of Electrical Machines," Goldschmidt, *Elektrot. Zeit*, 29, pp. 886, 912, 935, 1908; "Heating of Armatures of Electric Machines," G. Schmalz, *Elektrotechn. Zeitschr.*, 29, p. 188, 1908; "Heating of Ventilated and Enclosed Motors," Hartnell, *Inst. E.E. Journ.*, 41, p. 490, 1908; "The Heating of Induction Motors," A. M. Gray, *Amer. I.E.E. Proc.* 28, p. 605, 1909; "Heating of Armatures," G. Ossanna, *Elektrot. u. Maschinenbau*, 27, p. 489, 1909; "The Heating of Dynamos," E. Boulardet, *Rev. Electrique*, 15, pp. 508 and 552, 1911; "The Heating of Electric Machines," C. Caminati, *Lumière Electr.*, 15, p. 147, 1911; "Heating of Electrical Machinery," E. Hinlein, *Zeitschr. Vereines Deutsch. Ing.*, 55, p. 730, 1911; "Effect of Room Temperature on Temperature-rise of Motors and Generators," Day and Beekman, *Amer. Inst. E.E. Proc.*, 32, p. 415, 1913; "Effect of Air Temperature, Pressure and Humidity on the Temperature-rise of Electric Apparatus," Skinner. Chubb and Thomas, *Amer. Inst. E.E. Proc.*, 32, p. 553, 1913; "Internal Heating of Stator Coils," Williamson, *Amer. Inst. E.E. Proc.*, 32, p. 437, 1913; "Influence of the Cooling Medium on Temperature-rise of Stationary Induction Apparatus," Frank and Dwyer, *Amer. Inst. E.E. Proc.*, 32, p. 337, 1913.

PERMISSIBLE TEMPERATURES.

The temperature at which electrical machinery may run for long periods of time without suffering injury depends upon the character of the insulating materials used in its construction. The Sub-Committee on Rating of the American Institution of Electrical Engineers have divided the insulating materials into the following classes :

Class.

A 1. Fibrous materials which have not been specially treated for the purpose of increasing their mechanical strength or durability under high temperatures, such as cotton, paper and fibre. As a rule, such materials become brittle or lose their fibrous strength when subjected for a long time to moderately high temperatures.

A 2. Fibrous materials which have been subjected to a filling treatment with oil, gum, or similar substance which increases their mechanical resistance to disintegration. Impregnated cotton or paper fall into this class when the filling compound has not been so applied as to exclude air.

A 3. Fibrous materials which have been impregnated and "solid-filled" so as to exclude air.

B 1. Those insulations which consist mainly of mica or asbestos, cemented or impregnated with synthetic resins or other like material, whose presence fixes the permissible temperature, but in which the air is not excluded from the windings where they are employed.

B 2. Preparation of mica, micanite or asbestos applied to windings which are "solid-filled" with impregnating compounds so as to exclude air.

C. Fireproof materials, such as pure mica, porcelain, etc., for which no temperature limits are specified.

It is suggested that the highest temperatures to be permitted in a machine, in any part insulated with these materials, shall not exceed respectively the following :

Class A 1	90° C.
A 2	100°.
A 3	105°.
B 1	125°.
B 2	130°.
C	No temperature limit specified.

It would seem logical that the rating of a machine should be so fixed that when running continuously at its normal rating, or for a short period on overload, these temperatures should never be exceeded.

"Observable" temperature. Where temperature is measured by thermometer, or by the means ordinarily employed in temperature tests, it is very rarely possible to find the temperature of the hottest part. The highest measurable temperature will usually be smaller than the highest temperature attained in any part. The

highest temperature measured may be termed the "observable" temperature. The "observable" temperature will be less than the highest temperature attained in any part of the insulation by the amount of internal temperature drop. In practice, this internal temperature drop cannot be measured, but it can be arrived at approximately by the application of data which have been obtained by scientific investigation. One may ascertain approximately the highest temperature reached, by adding to the observable temperature a suitable number of degrees for the internal drop. The factors which control the amount of the internal drop are those which have already been considered in this chapter, p. 236. The American Institution of Electrical Engineers Sub-Committee suggest * the following approximate figures for the internal drop as a function of the voltage of the machine :

Up to and including 4000 volts - - - - - - - 10° C.
Above 4000 volts, and not exceeding 14,000 volts - - 20° C.

Thus they arrive at the following observable temperatures of winding for the rated pressures stated at the heads of the vertical columns :

	In windings of rotating apparatus, pressures up to and including 4000 volts.	In windings of rotating apparatus, all pressures between 4000 and 14,000 volts.
A 1	(90 – 10 =) 80°	(90 – 20 =) 70°
A 2	(100 – 10 =) 90°	(100 – 20 =) 80°
A 3	(105 – 10 =) 95°	(105 – 20 =) 85°
B 1	(125 – 10 =) 115°	(125 – 20 =) 105°
B 2	(130 – 10 =) 120°	(130 – 20 =) 110°

Permissible temperature rise. Logically, the permissible temperature rise can only be fixed if we know the temperature of the surrounding air in which the machine is intended to work. Thus, for a machine intended to run in a sub-station in a temperate climate, where the temperature of the surrounding air does not rise above 25° C., an actual temperature rise of 65°, or an observable temperature rise of 55°, would be permissible. On the other hand, where a machine is intended for a tropical climate, to work in a surrounding atmosphere which may at times reach 45° C., the observable temperature rise ought not to be more than 35° under the heaviest conditions of load.

In cases where the coils of a machine are very bulky, or, for any other reason, have a considerable temperature drop between the interior and the exterior (see page 236), the observable temperature rise should be even smaller.

* At the time of going to press, the result of the deliberations of the sub-committees of the International Electrotechnical Commission, the British Engineering Standards Committee, and the British Electrical and Allied Manufacturers' Association, had not been published. The consensus of opinion is, however, in general agreement with the suggestions of the Sub-committee of the American Institution of Electrical Engineers. The temperature of shunt windings should be ascertained by increase of resistance ; the temperature of armature windings may be obtained either by increase of resistance or by thermometer, and in the case of measurements by thermometer the temperature recorded should be 5 per cent. below that permissible when measured by rise of resistance. In the case of insulation of class B 2, the temperature as ascertained by increase of resistance should not be more than 115° C.

PART II

THE SPECIFICATION

AND

THE DESIGN TO MEET THE SPECIFICATION

PART II.

CHAPTER XI.

THE SPECIFICATION AND THE DESIGN TO MEET THE SPECIFICATION.

HAVING given in Part I. a general statement of the properties of the materials used in construction, and the rules which lead us to certain shapes and dimensions in the design of Dynamo-Electric Machinery, we will now consider the form of the specification which prescribes the performance of machines intended to be run under certain conditions. We will then proceed to apply the rules set out in Part I. to work out the details of machines designed to meet given specifications.

For each type of machine, whether it be A.C. or C.C. generator, induction motor or rotary converter, there will be many different circumstances arising in connection with the purpose for which it is used, which will lead to the specification of definite qualities in the machine.

Given the duty that has to be performed, certain qualities should be called for in the specification, and it will be part of our business in this section of the book to show how the specification should be worded so as to describe what is wanted, without interfering with the province of the designer and the manufacturer.

Then, given a certain specification calling for definite qualities in the machine, another part of our duty will be to show how the manufacturer might design the machine so as to give it those qualities in the most economical and satisfactory way.

It will be convenient for this purpose to take each class of machine in order, and consider two or three machines in each class intended for work calling for widely differing characteristics, and after giving the purchaser's specification in each case, to work out a design fully to meet that specification. But, first, we will make some general remarks upon the purchaser's specification.

PERFORMANCE SPECIFICATIONS IN GENERAL.

Main object of specification. A purchaser's specification of a dynamo-electric machine should aim mainly at stating the duty that it is intended to perform, the conditions under which it will operate, and the tests that will be applied in order to ascertain whether the performance is satisfactory. It should leave to the manufacturer considerable licence to adopt such methods of construction as he may prefer, in order to obtain a machine which shall fulfil the prescribed conditions.

For instance, it is much more important to state that a machine is intended to operate in an engine-room having a temperature of 110° F. in a damp climate than it is to specify " that the coils shall consist of copper wire having a conductivity not less than 98 per cent. of Matthiesson's standard," " that the current density in the conductors shall not be more than 1500 amperes per square inch in the armature coils and 2000 amperes per square inch in the field coils," and " that the armature shall be built up of thin laminations of Swedish iron."

The manufacturer, for his own protection, will use copper of high conductivity (generally of 100 per cent. of Matthiesson's standard). Copper of high conductivity is very ductile and less liable to break when bent around corners than copper of lower conductivity. The clause as to conductivity comes down to us from the early telegraphic cable days, when it was necessary to instruct the manufacturer in his art. Then, again, the current-density at which it is advisable to work the copper in any part of a machine depends largely upon the cooling conditions. It will often be found that in shunt coils the manufacturer cannot work the copper higher than 800 amperes per square inch if he is to meet the temperature guarantees, while in some parts he may, with impunity, employ 3500 amperes per square inch, and yet give a thoroughly cool and satisfactory machine.

Of course, where the purchaser has a preference for some particular type of construction, or for the use of a particular grade of material, it is important that he should state his preference, always giving the manufacturer the opportunity of substituting some other construction or material, which he can demonstrate to be better adapted to the machines as manufactured by him.

The purchaser is interested in the qualities of the materials used in so far as those qualities affect the permanent character of the work. Thus he may usefully specify the tensile strength of materials employed, or object to certain methods of insulation which experience has shown to be treacherous.

Arrangement of clauses. It is important that the specification should have its clauses arranged in such a manner that matters of the same character are dealt with together and in natural sequence. It often happens that in the perusal of a specification by the staff of a manufacturing firm, different parts of the specification are dealt with by different individuals, and it therefore contributes not only to good feeling on the part of these individuals, but also to the efficiency of the specification in stating the matter in hand, if each man who reads it, finds the parts with which he is concerned without having to read through all the clauses.

A good way to begin is to state the kind of machine required, whether generator, motor or rotary converter, and then give in tabular form the rating, so that anyone can see at a glance the size and character of the machine required. For instance, in specifying an A.C. generator, one might put the data of its rating in tabular form as follows:

ENGINE-DRIVEN ALTERNATING-CURRENT GENERATOR.

Normal output - - - - -	1250 K.V.A., 1000 K.W.
Power factor of load - - - -	0·8.
No. of phases - - - - -	3.

Normal volts - - - - - -	6300.
Voltage variation - - - - -	6000 to 6600.
Amperes per phase - - - -	115.
Speed - - - - - - -	250 revs. per min.
Frequency - - - - - -	50 cycles per second.
Regulation - - - - - -	8 per cent. rise with non-inductive load thrown off.
Over-load - - - - - -	25 per cent. for 2 hours, and 50 per cent. for 15 minutes.
Exciting voltage - - - - -	125.
Temperature rise after six hours' full load	40° C. by thermometer. 50° C. by resistance.
Temperature rise after two hours' 25 per cent. over load	55° C. by thermometer. 65° C. by resistance.
Puncture test - - - - -	13,000 volts (alternating) applied for 1 minute between armature coils and frame. 1000 volts (alternating) applied for 1 minute between field coils and frame.

This list of particulars is not intended to give full information; it is merely intended to give at a glance the general rating of the machine required.

Or, if the machine in question were a rotary converter, the principal data might be set out as follows:

ROTARY CONVERTER.

Normal output - - - - -	500 K.W.
Number of phases - - - -	6.
Frequency - - - - - -	50 cycles per second.
Continuous-current voltage - - -	550.
,, ,, amperes - - - -	910.
Running A.C. to C.C. - - - -	Yes.
,, C.C. to A.C. - - - -	Yes.
Compounding - - - - -	530 to 550.
Adjustment of voltage on rheostat -	500 to 550.
H.T. power factor at 550 volts full load	0·97 leading.
Over-load - - - - - -	25 per cent. for 3 hours. 50 per cent. for 10 minutes.
Temperature rise after six hours' full load	40° C. by thermometer. 50° C. by resistance.
Temperature rise after three hours' 25 per cent. over load	55° C. by thermometer. 65° C. by resistance.
Puncture test - - - - -	1500 volts (alternating) for 1 minute between windings and frame.

It is then convenient to make a general statement of the purposes for which the machine is required, such as the nature of the load to be supplied, the machines (if any) with which it is necessary to run in parallel, the location of the power house or other running position, and any circumstances which may make the performance difficult.

Then may follow clauses giving fuller particulars of the electrical rating, such as will be found in the model specifications given in this book.

Finally, care must be taken to state exactly how much the specification is intended to cover, in the matter of erection and setting to work, in the matter of foundations and the provision of connecting cables and auxiliary appliances.

CHAPTER XII.

ALTERNATING-CURRENT GENERATORS.

HIGH-SPEED-ENGINE TYPE.

ALTERNATING-CURRENT generators differ somewhat in their construction, according to the service for which they are intended. In the first place, the prime mover employed may be any of the following: a slow-speed engine (perhaps a gas engine), a high-speed steam engine or motor, a water turbine, or a steam turbine, and it will be necessary to adapt the design of the machine to speeds which are suitable for these various prime movers. Secondly, the kind of load will vary in different cases. We may have an intermittent motor load of low-power factor, with or without lighting in parallel, or we may have a steady lighting load, or we may be called upon to deliver current at a widely varying voltage, as to an electric furnace. The size of the other units running in parallel and the size of the general system of distribution will also influence us in prescribing the characteristics which the generator under consideration must have.

We will therefore consider here four well defined types of alternate-current generators:

(1) A 750 K.V.A., three-phase, 50 periods, 2100 volts generator, designed to be driven by a steam engine running at 375 R.P.M., and to supply a load consisting of induction motors varying in size from 10 to 100 H.P. with 50 K.W. of lighting load in parallel.

(2) A 2180 K.V.A., three-phase, 50 periods, 6300 volts generator, intended to be driven by a gas engine at 125 R.P.M., and running in parallel with similar machines supplying a lighting and traction load.

(3) A 2500 K.V.A. generator driven by a water turbine at 600 R.P.M., and generating three-phase current at 6900 volts, which is to be transmitted over a line to various sub-stations for the supply of municipal lighting and power.

(4) A 15,000 K.V.A. machine driven by a steam turbine at 1500 R.P.M., and generating three-phase current at 11,000 volts for general municipal supply.

In each of these cases we will consider the characteristics which the generator should have, and then give a suitable specification. We will then work out a design of a generator to meet the guarantees asked for in each specification. In connection with each design, it will also be possible to consider what variations might be made to suit possible variations in the conditions.

There are one or two matters affecting all A.C. generators that should be discussed before we pass on to the individual specifications.

Regulation. The inherent regulating quality of the machine to be asked for will depend upon the character of the load and upon the number and output of the generators in the power house. Where the output of the station is not large, and the load is unsteady, a machine of fairly good regulation will be specified. But where the changes in load are small compared with the total output, a cheaper machine of poorer regulation may be specified. For a mixed power and lighting load, in which the lighting is of first importance, it is usual to install an automatic regulator. Even a machine of 6 per cent. regulation is hardly steady enough when motors are started. In cases where the lighting is of secondary importance, it is quite common practice to install a machine of such inherent regulating qualities as to give 8 per cent. rise in voltage between full-load unity power factor and no load, and, say, 22 per cent. rise in voltage between full-load 0·8 power factor and no load. Such a machine, if installed without an automatic regulator, might commonly show on the voltmeter a drop of 10 per cent. to 15 per cent. when large motors are started up. This voltage drop would not be of great importance if we are not concerned with the lighting. If, however, it is important to keep the incandescent lamps steady, a regulator of one of the well-known types will be installed. Where a regulator is used, a generator of somewhat poorer regulation than that mentioned is often installed; but in view of the fact that no regulators are instantaneous in action, or are immune from getting out of order, it is better practice to install a machine of fairly good regulation—say with not more than 8 per cent. rise in voltage between full load and no load, and 12 per cent. drop in voltage between no load and full load on unity power factor. Where the generator is to work alone, an even better regulation will be preferred by some users, if it can be obtained at a reasonable price, and there is no doubt that the operation of the plant is somewhat more satisfactory. A generator of good regulating qualities can, without much additional cost, be given a very much greater over-load capacity than a machine of poor regulation. Many cases have arisen in which good regulating generators have had their armatures rewound, and the capacity increased two-fold. Where the load on a station has increased beyond expectation, the fact that ample machines were originally installed had resulted in a great saving of money. Where three or four generators of fair size are working in parallel, and the large induction motors are all started up on resistances so that they do not make heavy demands for wattless current, it will be found that generators giving not more than 25 per cent. rise in voltage when full inductive load is thrown off are in general satisfactory. It is not very important that all the generators running in parallel shall have the same regulating qualities. The only effect of running a good regulating machine in parallel with a poor regulating machine is that the good machine tends to take more than its share of the wattless current as the load increases.

Temperature rise. The main object to be kept in view in specifying temperature rise is to ensure that in the ordinary course of operation no part of the machine shall attain a temperature which will permanently injure it. It is, of course, impossible to actually ascertain the temperature of internal parts, and one can only use judgment based on past experience. Generally, it may be stated that

where the insulation is thick, as in high-voltage machines, there will be a tendency for the temperatures attained by internal parts to be much higher than those measured by thermometer. In machines of 11,000 volts, one may assume that with ordinary methods of construction the temperature on the inside of an armature coil will be about 20° or even 25° higher than the temperature of the surrounding iron; whereas in most low-voltage machines there will commonly be not more than 10° difference in temperature between the inside of the insulation and the surrounding iron. The internal layers of wire-wound coils (see page 236) are often very much hotter than one might commonly suppose from a measurement of the average temperature by the increase of resistance method. Where the kind of machine (*e.g.* a low-frequency alternator of high speed) is such as to employ bulky field-coils, special care will be employed in the specification to prevent excessive temperatures in the internal layers. There is some difference of opinion as to how high a temperature such insulating materials as cotton and paper will satisfactorily withstand for long periods of time. Although the cotton covering of wire may be subjected to a temperature of 125° C. for long periods without being destroyed, there is no doubt that any temperature over 100° C. will dehydrate the cellulose in time, and render it extremely brittle. In parts where the construction is such that movement of the conductors relatively to each other may occur as in revolving armatures, the temperature should not be allowed to exceed 100° C., but in stationary coils not subjected to vibration the internal temperature often exceeds 120° C. without any apparent harm. If we take 90° C. as a safe temperature for cotton-covered wires of a revolving field coil at normal loads, we can arrive at the allowable temperature rise measured by thermometer as follows: Deduct from 90° C. the number of degrees—10 to 25—by which the actual temperature may exceed the measured temperature, and then from the remainder deduct the temperature of the air which we expect to find in the power house. For instance, the part of a 600 K.W. A.C. 440 volt revolving-field engine-type generator which is most likely to deteriorate from excessive temperature is the cotton insulation on the field coils. The inside of a field coil of a machine of the above rating might be 20° C. hotter than the temperature measured by thermometer. This is assuming square wire coils of about 1″ winding depth (see page 236). Deducting 20° from 90°, we get 70°, and assuming the temperature of the air in the power house to be 25° C., we may safely allow 45° temperature rise in the field coils measured by thermometer. If the temperature were measured by the resistance method, we might safely allow 55° C. rise.* It will be seen that where a machine is intended to operate in a hot climate, and where the temperature of the power house might for long periods be at 45° C., it would be well to specify 30° C. rise for a low-voltage machine. For high-voltage machines, it might be justifiable to call for a lower temperature rise. It must, however, be remembered that the figure of 90° C. is rather on the safe side, and one might base one's figures on 100° C. as the permissible temperature of the very hottest part. It depends on the relative importance of the reliability of the machine and its first cost (see page 256).

* In revolving-field coils the ends of the coils exposed to the draught are rather cooler than the parts between the poles, so that the average temperature of all the copper is not much higher than the hottest spot that can be found by the thermometer.

When a machine is run on 25 per cent. over-load, the losses in the armature copper are increased about 60 per cent., but one does not ordinarily find that the temperature of the armature copper is increased by 60 per cent., because the losses due to air friction and iron loss do not increase appreciably with the load.

If a generator is of fairly good regulation, so that the field current is only increased 30 per cent. between no load and full load, the increase of field current will be about 40 to 45 per cent. between no load and 25 per cent. over-load (see page 292). That is to say, the field current may be increased $11\frac{1}{2}$ per cent. above the full-load value. We may expect a temperature rise 32 per cent. higher when at 25 per cent. over-load, if we have an increase of the field current of $11\frac{1}{2}$ per cent. and an increase of the resistance 6 per cent., because $1{\cdot}115 \times 1{\cdot}115 \times 1{\cdot}06 = 1{\cdot}32$. So we see that, if the field coil is the part which we expect to be hottest on over-load, 60° C. (as measured by thermometer) would be a reasonable temperature rise to allow for this over-load. The temperature rise in the hottest part of the coil might reach 87° C. above the atmosphere, if the over-load were maintained continuously.

Efficiency. In cases where an engine and generator are bought from one contractor, the purchaser is generally not concerned with the efficiency of the generator itself, so long as the steam consumption of the set per K.W. hour is guaranteed. In many cases, however, the generator is sold by a contractor who has no responsibility for the efficiency of the engine; in this case the efficiency guarantees are important.

It should be clearly stated how the efficiency is to be arrived at. Most manufacturers specify that the efficiency shall be calculated from the various losses (copper losses, iron losses, etc.) measured separately. In this case, it should be clearly stated what friction and windage are to be included among the losses. Some makers will exclude all bearing losses when the bearings are supplied by the engine builders. Others will include the losses in the outboard bearing. Where there is a flywheel, the windage loss due to it will in general be included in the losses of the maker who supplies the flywheel. In general, it is not good practice for the purchaser to specify any particular efficiency. It is better to ask the contractor to state his efficiency, calculated in a certain way. The losses to be included should be clearly stated. The K.V.A. output of the machine, the power factor of the load and the voltage at the terminals at which the machine is supposed to be run when the efficiency is taken should also be stated. In allowing for increase in the resistance of the conductors, it is sometimes assumed that they will reach the temperature rise specified, though in cases where the copper is found on full load to be very much below the specified temperature, the contractor is entitled to take his "hot" resistances at the temperatures actually reached. This is of special importance in the case of slow-speed engine-type generators having a very great number of poles, because in these cases the temperature of the field is generally fairly low, on account of the very large cooling surface (see p. 347).

Excitation. If there is always available in the power house a continuous-current supply of a voltage not higher than 240, it is quite good policy to excite from this supply, and an exciter may be added as a spare. The advantage of exciting the field-magnet from a supply of constant voltage is that the exciting current is then independent of the speed of the engine, and the regulation of the set is therefore

better. It is not, however, good practice to excite an alternating-current generator from a 500 volt C.C. circuit (unless the output is very large), because the economical size of wire to be employed is rather small for use on revolving field coils.

The most common method is to provide an exciter directly connected to the end of the shaft of the main generator. This exciter will cost a little more than a belted one running at a higher speed, but is generally considered more satisfactory. Where an exciter is employed, the voltage chosen will generally be 125 volts. In some cases where an automatic regulator is installed, an exciter is a necessary part of the equipment.

Rheostats. It is quite common practice to have no rheostat between the exciter armature and the field-magnet, and to rely entirely upon a rheostat in the field circuit of the exciter to obtain the necessary change in excitation. This arrangement renders the regulation of the set much poorer than where a main rheostat is installed, because the exciter voltage will change more with speed when it is working with its field not fully excited than where it is working at full voltage. The use of a main rheostat, of course, leads to some extra loss, which in the case of a 600 K.W. generator at 375 R.P.M. would amount to be about 2 K.W. at half load, if the exciter were always maintained at its full voltage.

High-voltage test. The purchaser's specification will state the testing voltage, which will be applied between the armature winding and frame, and between the field winding and frame. It will also specify the interval of time during which the testing voltage is to be applied.

These matters may be in accordance with the rules laid down in Chapter VIII. page 188. In case the working voltage in the armature is 2100, a suitable testing voltage would be 5000 applied for one minute. If the field is excited at 125 volts, a suitable testing pressure would be 1000 volts applied for one minute.

After these general remarks, we will proceed to make out a specification for a 750 K.V.A. three-phase generator for 50 periods, 2100 volts, 375 R.P.M.

A machine of this character would, in all probability, be built on one of the standard frames of the manufacturer; and if we wish to purchase a cheap machine, it is desirable to avoid anything in the specification which will prevent a manufacturer from quoting on his standard plant. The specification will therefore, in this case, be as short as possible, and will not contain anything more than is necessary to secure a generator which will satisfactorily perform the work intended for it.

SPECIFICATION No. I.

750 K.V.A. THREE PHASE ENGINE DRIVEN GENERATOR.

1. The work covered by this specification is to be carried out in accordance with the General Conditions and Regulations issued by the Institution of Electrical Engineers, in so far as they are not inconsistent with anything contained herein. General Conditions.

Extent of Work.

2. The work includes the supply, delivery, erection, testing and setting to work on the site shown in the accompanying drawing No.   of an alternating current generator which shall have the characteristics set out below :

Characteristics of Generator.

Normal output	750 K.V.A., or 600 K.W.
Power factor of load	0·8.
Number of phases	3
Normal voltage	2050
Voltage variation	2000 to 2100.
Amperes per phase	206.
Speed	375 revs. per minute.
Frequency	50 cycles per second.
Regulation	8 per cent. rise with non-inductive load thrown off, the speed and excitation being constant.
	22 per cent. rise with 0·8 power factor load thrown off, the speed and excitation being constant.
Over load	255 amperes at 2050 volts power factor between 0·9 and unity.
Exciting voltage	120.
Temperature rise after 6 hours full load	45° C. by thermometer.
	55° C. by resistance.
Temperature rise after 2 hours over load	55° C. by thermometer.
	65° C. by resistance.

Running Conditions.

3. The generator is intended to run in parallel with two generators of similar output and speed at present installed in a power-house, supplying power to a colliery, the most distant parts of which are about three miles away. The load will consist of coal-cutters, three-phase haulage motors and the lighting of the mine. The largest motors at present installed are 100 H.P., and are of the slip-ring type. It is proposed to install a 400 H.P. winding motor of the slip-ring type. The generator shall be suitable in every way for taking this class of load.

Nature of Load.

Type of Generator.

4. The generator shall be of the revolving field type, and the spider shall be mounted in such a manner that it can be very rigidly fastened to the flywheel of the engine. The method of attachment shall be indicated in the outline supplied with the tender.

Connection to Engine.

5. With the generator the contractor shall supply a bedplate adapted for bolting to the engine bedplate, and an outboard bearing. The bearing shall have a self-aligning seating and be provided with approved means of adjustment. **Bedplate and Bearings.**

6. The foundations will be supplied by the purchaser to templates furnished by the contractor. The contractor shall supply all foundation bolts. Within four weeks after the acceptance of his tender, the contractor is to provide a drawing showing the details of the bedplate and foundation bolts, and the position of the terminals of the generator. Cables from the generator terminals to the switchboard will be provided by the purchaser. **Foundations.** **Cables.**

7. The contractor is warned that the engine-room is in a dirty situation, and the machinery supplied must be suitable to run under the existing conditions. **Severe Conditions.**

8. There is a railway track into the power house, and an overhead hand-operated crane capable of lifting loads of 10 tons from a railway truck to the proposed foundations. The contractor may have the use of this crane at his own risk, and he shall be responsible for any damage done. The tender shall state the maximum weight to be lifted during erection or overhauling. **Access to Power-house.** **Crane.**

9. The generator shall run well in parallel with the existing generators, which run well in parallel with one another. **Parallel Running.**

10. The electromotive force wave form of the generator shall at full load be a smooth even curve* free from ripples or pronounced higher harmonics. **E.M.F. Wave.**

11. The generator shall be excited from the existing exciting bus-bar at 120 volts, and no exciter need be provided. The exciting bus-bar pressure may at some future time be controlled by an automatic regulator to maintain the A.-C. voltage of the station constant, but the inherent regulation of the generator must be as specified above, apart from any automatic control. **Excitation.**

12. The tenderer shall state in the tender what provision is made for obtaining access to the field magnet and armature for inspection and repair. He shall also state the method proposed of replacing armature coils and field coils in case of a breakdown. **Access for Repair.**

* See page 380 for a more stringent clause.

Short-circuit. 13. The generator must be able to withstand a short circuit at its terminals when running at full voltage, but the contractor shall not be called upon to carry out a short circuit test.

Permanent Construction. 14. The contractor must be able to show by calculation that the mechanical strength of all parts is such that when running at full speed on load there is a factor of safety of four. No part of the generator shall be of a material which will deteriorate with time, and become so weak, brittle or otherwise defective as to make the factor of safety less than four.

Oil-throwing. 15. The oil-throwing devices on the shaft and bearing shall be so efficient that no oil or oil vapour is apparent outside the bearing during ordinary running without attention. After erection and final adjustment, a special test shall be made to see that this condition is complied with.

Efficiency. 16. The efficiency shall be calculated in the following way: The iron loss at 2100 volts and the friction and windage shall be measured at no load. The armature resistance shall be measured at a known temperature and the I^2R loss calculated at 60° C. The field and rheostat losses shall be taken as together equal to the number of amperes of field current at 0·8 power factor multiplied by 120, the voltage of excitation. All the above losses, expressed in kilowatts, shall be added to the kilowatt output, and the ratio of output to this sum shall be taken as the calculated efficiency. The contractor shall state in the schedule attached the efficiency of his generator calculated in this way at full, three-quarter and half load on a power factor of 0·8, and he shall guarantee that there shall be nothing in the construction of the machine that will make the actual efficiency when running on load more than 1 per cent. less than the figures so stated.

Rheostat. 17. A field rheostat and multi-contact switch is to be provided in the field circuit of the generator, of sufficient capacity to lower the voltage of the armature to 1950 volts at no load when the machine is cold. Sufficient contacts must be provided on the switch to make the voltage change very gradual as the switch is moved over the whole range. One step of the rheostat must not change the voltage by more than 15 volts at any load and at any part of the range when the machine is operating by itself.

18. The slip-rings for the exciting circuit must be of sound metal free from blowholes and mounted in a manner that ensures exact concentric running. The brush-holders must be of a solid, simple construction, rigidly supported, and so made that the brushes can be easily inspected while the machine is running. There must be at least two brushes per ring (preferably at opposite ends of a diameter on each ring), and there must be no heating or sparking at the rings with the maximum field current flowing and one of the brushes raised. The brushes must be of carbon. **Slip-ring and Brush-holders.**

19. The following tests shall be carried out on the generator:

(*a*) Measurements shall be made of the resistances of the armature and field windings. **Tests of Resistance.**

(*b*) The generator shall be run at full speed at no load with the field excited, and measurements shall be taken showing the relation between field current and voltage generated, the iron loss at various voltages, and the friction and windage. **Magnetization Curve.**

(*c*) The generator shall then be run with the armature short circuited, and measurements taken to show the relation between the field current and the armature current. **Short-circuit.**

(*d*) From tests (*a*), (*b*) and (*c*) the field current required at full load 0·8 power factor shall be approximately calculated, and the generator shall be run at this field current for six hours, and measurements taken of the field resistance while hot. **Field-heating Run.**

(*e*) While the machine is still hot an alternating pressure of 5000 volts virtual shall be applied between the armature winding and frame for one minute, and an alternating pressure of 1000 volts between the field winding and frame for one minute. **Puncture Tests.**

(*f*) After erection on site or at the engine-builders' works, as shall mutually be agreed upon, the generator shall be run at full load, 0·8 power factor, for six hours, and for two hours on the stated over load, and measurements shall be taken of the temperature of the armature windings and iron, and field windings, by thermometer, and of the field windings by resistance, to see that the specified temperature rises above the surrounding air are not exceeded. For the purpose of these tests the **Full-load Temperature Run.**

temperature of the engine-room shall be taken three feet away from the generator in a line with the shaft.

Regulation. (*g*) If the purchaser is not satisfied with the calculated regulation figures obtained from tests (*a*), (*b*) and (*c*), a regulation test shall be made on site after erection by throwing off full load at 0·8 power factor to see if the generator has the inherent regulation specified.

Endurance. (*h*) After erection on site the generator shall be run on its ordinary daily load for one week under the direction of contractor's engineer to see that all matters are in order. It need not be accepted by the purchaser until it is complete in every particular.

Spares. 20. The tenderer shall quote separate prices for the following spare parts :

(1) A field coil.

(2) Armature coils of various sizes.

(3) Brush gear and brushes.

(4) Bearing bush.

THE DESIGN TO MEET THE SPECIFICATION.

We will now consider the matter from the **manufacturer's point of view.** We will suppose that he has received an order for a 600 K.W. generator, which is to comply with the above specification. How can he most economically build the machine ?

Choice of frame. The particular length and diameter of frame that he will choose will depend upon what machines of similar size he has built before, and the patterns and dies that he has available. It may be much cheaper for him to choose a D^2l much larger than the theoretical minimum than to build an entirely new machine which shall employ the smallest possible amount of material. All that we can do here is to choose a diameter and length which will be very economical in material, and yet sufficient to enable a machine of simple construction to be built which will safely meet the guarantees.

Depends on output of field-magnet. It will be found that with revolving field A.C. generators of fairly good regulation the limiting conditions as to size lie in the field-magnet. We must have a certain cross-section of steel in the poles to provide the magnetic flux without undue saturation, and we must have sufficient copper space for the field ampere-turns at full load. At the same time, we must provide air spaces for cooling between the field coils. These considerations determine the size of the field-magnet. If we are sure that this is big enough, there will be no difficulty with the diameter and length of the armature, because it will be found that a reasonably shallow slot (2 to 2½ inches deep) will carry all the copper we

want in the armature, and there will not be much difficulty in providing sufficient cross-section in the teeth to carry the magnetic flux, except in very high-voltage machines.

We will consider first **the field-magnet**. It has been found that one of the most economical constructions for high-speed engine-type generators having from 6 to 20 (or even more) poles is one consisting of a cast-steel spider with **poles of mild steel** machined out of the **solid** bar and bolted on. For very high speeds, as for water turbine-driven generators, the poles may be dovetailed in.

Cast-steel poles are sometimes used, but they are not always free from blow holes. If the sides of the poles are not machined, a considerable amount of space must be allowed on the inside dimensions of the field coils to allow for roughnesses of the casting. This is a bad feature. If the poles are machined on the sides they will cost as much as, or more than, poles cut out of solid mild steel.

Punched poles. Where a manufacturer has a pole die of the right size, or where the number of poles is so great as to make the cost of a new die of little importance, he can build up poles out of punched steel just about as cheaply as he can machine poles out of the solid, for the overhanging horns of the pole necessitate the machining away of a large quantity of metal.

The **punched poles** have the **advantage** that they can be used with open slots in the armature without causing so much loss in the pole face. Where open slots are used, and particularly where the width of the slot is more than double the width of the air-gap, laminated poles should be used, or at least laminated pole shoes.

When a pole is built up of punchings, it is comparatively easy to provide it with tunnels near the pole face for the reception of copper rods to form a damper, or to make slots in the iron to cause any required amount of saturation, or otherwise make the pole of a complicated shape that might be expensive to make out of solid metal. On the other hand, when once a die is made, we are to a great extent restricted in our design by the shape of the punching. We cannot, without expense, for instance, narrow the pole to make room for more copper in a case where that course might be advisable, or widen the pole in a circumferential direction to get in more iron in cases where the saturation is rather high. We can, however, always build up a punched pole to a greater axial length when we wish to increase the cross-section of the pole body.

Relation of width of pole to pole pitch. In designing a pole die for a particular frame, it is of great importance to make the width of the pole body in relation to the pole pitch such that we can get the most economical arrangement of material for those generators that are most commonly built on that frame. In the first place, we need hardly say that there is a great advantage in using a pole with **overhanging lip**, as in Fig. 234. This lip enables us to make the pole face as wide as we like, while we have a free hand with the width of the pole body. The lip also gives a good mechanical support for the coil. It will be found that there is no advantage in making the pole arc greater than two-thirds of the pole pitch except in high-speed machines, where the coils are difficult to support.

Relation of pole arc to pole pitch. The only object in widening the pole arc is to reduce the ampere-turns on the gap, but it is better to make a short air-gap

than to widen the pole arc unduly. The magnetic flux which comes from the pole lips is out of phase with the flux in the centre or the pole, and is therefore not very effective in producing useful electromotive force, while all the flux that comes from the pole requires so much cross-section of iron in the pole body.

The bringing of the lips on North and South poles near together increases the magnetic leakage, and by taking away from the usefulness of the pole body may diminish the output of the machine. Upon the whole, on 50 cycle engine-driven generators a pole arc about 0·64 of the pole pitch will be found to be most generally useful. If the corners of the pole are bevelled off as in Fig. 334, it will be found that the fringing from the lips and sides of the pole brings up the field-form constant K_f to 0·64 (see page 16). The electromotive force constant K_e for a three-phase winding of full pitch, and not less than two slots per phase per pole, would with this pole be 0·4. For a two-phase winding, the constant K_e would be 0·315.

Width of the pole-body. We now come to a most important consideration affecting the output and performance of an A.C. generator. In the first place, it will be observed that most manufacturers employ a pole body with **parallel sides.** This is because it is so much easier to wind the coil for a pole with parallel sides, and to slide it on, than to make a coil to fit a taper pole and hold it in position. Nevertheless, the **taper pole** has some strong claims if we wish to get the greatest possible output from a given amount of material.

The **drawback** to the pole with **parallel sides** is that if we make the pole body immediately below the lips as narrow as we would like to make it, and thus get plenty of room for copper, we will find that the width at the bottom is too small, and the saturation of the iron, particularly at heavy loads, is too great. If, on the other hand, we make the pole at the base as wide as we would like to make it, we have more iron than we need at the top of the pole, and we are cramped in our copper space. If we use parallel sides, we must make a compromise, keeping the saturation within sufficiently safe limits, and yet getting as much room for copper as we can.

The best width for a parallel pole depends upon the number of poles. Where a machine has many poles, the centre lines of adjacent poles are nearly parallel, and this gives more room at the root than where, the number of poles being few, the centre lines are inclined to one another. For machines with 12 poles of moderate regulating qualities the pole body is usually made about half the pole pitch. As the number of poles is increased, the ratio increases from 0·5 to 0·6. For 8 poles the ratio is often as low as 0·47 and for 6 poles as low as 0·45.

The above figures are based on the assumption that copper costs 8*d*. per pound and iron 1*d*. per pound. If the cost of copper increased very much, it would pay to reduce the copper space and to somewhat increase the size of the whole frame, putting in more iron.

Relation between weights of copper and iron. To arrive at clearer ideas as to how the output of a frame depends upon the amount of copper and iron in it, we may consider one of the poles of the 16 pole case given in Fig. 234. For the same peripheral speed and length of iron the output of the machine is proportional to the number of poles, so that we may consider one pole by itself, and aim at

making the proportions between the iron and copper such as to get the greatest output for a given cost.

We have

$$\text{output of three-phase generator} = \text{volts} \times \text{amps.} \times 1{\cdot}73,$$

and from equation (1), page 24, we have

$$\text{volts} = K_e \times R_{ps} \times A_g\text{B} \times 10^{-8} \times \text{conductors}.$$

Therefore

$$\text{output} = (K_e \times R_{ps} \times A_g\text{B} \times 10^{-8}) \times (\text{conductors} \times \text{amperes}) \times 1{\cdot}73.$$

Now K_e depends on the pole-arc, but not necessarily on the pole-body width, so we can leave it out of account in the present discussion. The two important factors are:

(1) $A_g\text{B}$ = "magnetic loading."

(2) Conductors × amperes = $Z_a I_a$ = "current loading."

Now, the magnetic loading $A_g\text{B}$ can be increased by increasing the width of the pole body. Within the limits of practical design $A_g\text{B}$ will be roughly proportional to the width of the pole body.

For machines having the same ratio between field ampere-turns and armature ampere-turns, the factor $Z_a I_a$ can be increased by increasing the copper space of the field. The increase of $Z_a I_a$ will not be quite proportional to the increase in the copper space, because the cooling conditions are worse with increased depth of copper, but for very small increases in the copper space we may, for the sake of the present discussion, take $Z_a I_a$ as proportional to the copper space.

Now consider Fig. 234. The half width of the pole is 3·125 and the mean depth of the copper winding 1·25 inches. Suppose that we were to take 0·1″ off the side of the pole and utilize for copper the space gained. It is clear that the flux factor would be reduced by a little over 3 %, while the copper factor would be increased by 8 %. Even allowing for the cooling conditions being somewhat worse, it is clear that the output of the frame can be increased by making the pole narrower and using more copper. But at what cost? The extra copper for field and armature will cost, say, 8*d.* per lb., while the saving in iron is very little. A more economical way of increasing the output would be to leave the copper weight as it is, and to increase the width of the pole. This would mean using a larger frame, but an increase of the iron by 5 % right through the machine would not cost as much as an increase of the copper weight by 8 %. As a matter of fact, the problem of arriving at the best proportion between copper and iron is so complicated by various considerations, such as the cost of labour, the freights on foreign shipment and the expediency of standardization, that it is impossible to get an exact solution. It is, however, clear from the machines put on the market by the most successful makers, that it does not pay to get the greatest theoretical output from a frame of given size. There comes a time in the loading of the frame when the money put into extra copper is better spent in increasing the size of the frame. The proportions given in Fig. 234 are not far from what is practically the most economical arrangement.

Now, the winding shown on the pole will (when the axial length of the machine is about 12″) carry about 10,000 ampere-turns for 45° C. rise, the peripheral speed

being 6000 feet per min. (see page 303). We may therefore take 10,000 ampere-turns as full-load ampere-turns on the pole. What shall we take for the no-load ampere-turns? That depends upon the inherent regulation asked for, and leads us to some general remarks on regulation.

THE REGULATION OF A.C. GENERATORS.

One of the main considerations which determine the size and cost of an A.C. generator is its quality of maintaining its voltage within narrower or wider limits, commonly spoken of as its "inherent regulation." Before we can fix upon the size of the frame upon which to build a generator of a given output and speed, we must see within what limits it is required to maintain its voltage when the load changes.

The most usual way of specifying regulation is to give the percentage *rise* of voltage when the load is thrown off, the speed and excitation being kept constant. We will speak of this as "regulation up." As the iron of the field-magnet usually becomes saturated* at voltages a little above the normal, much closer regulation figures can be guaranteed when the regulation is specified in this way than when the percentage *drop* in voltage, when the load is thrown on, is specified. The latter characteristic we will speak of as "regulation down."

From the user's point of view a machine with 8 % regulation down is a much more satisfactory machine than one with 8 % regulation up, but the cost of the first machine will be considerably higher, so that unless the load is of a very fluctuating nature, and it is required to maintain the voltage fairly steady, independently of the action of an automatic regulator, the purchaser will be content to take the guarantee most commonly offered by manufacturers.

The methods of predetermining the regulation of an A.C. generator from the design data and from no-load tests, have formed very fruitful subjects for discussion in our text-books, and in papers before learned societies. No method known to the author is perfectly accurate when put to the practical test. All methods assume a sine-wave form for the armature current, and none take into account in a perfectly satisfactory manner the bevelling of the pole face or the saturation of the iron. Fortunately, in ordinary commercial design it is not necessary to predetermine the regulation of a generator very closely. So much variation occurs, even between two machines built to the same drawings, that considerable margin must be allowed if the regulation guarantee is to be met with certainty, and therefore a superfine method of calculation is out of place.

The method which we shall give here is one which has stood well the test in practical manufacturing, and while probably as accurate as any other for generators with cylindrical field-magnets,† it is very easy to apply and to understand.

Consider a two-pole generator provided with a cylindrical field-magnet, such as is generally found in high-speed turbo-generators. The field winding usually occupies some 75 or 80 per cent. of the circumference. This is indicated by the

* There are other matters beside the saturation which tend to make the regulation down much wider than the regulation up. These are considered later.

† The case of generators with salient poles is considered on page 293.

dots and crosses on the inner circle in Fig. 300. A dot represents a current coming towards the observer, and a cross a current going away. The regulating qualities of the machine will depend to a certain extent upon the width of the pole.

We will consider a three-phase generator, because this is the kind of generator most commonly built, and its armature reaction behaves as a rotating vector of almost constant value.

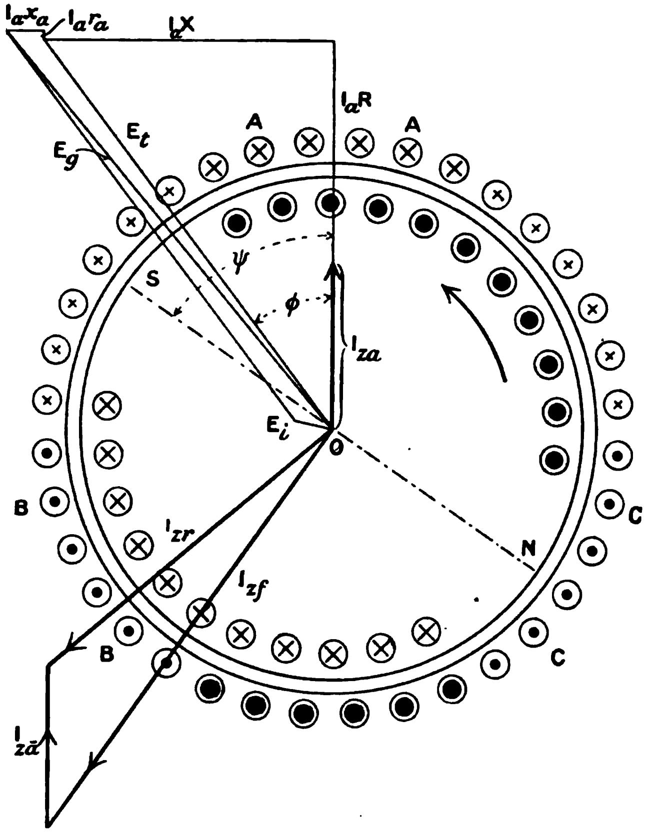


FIG. 300.—Showing the effect of armature reaction on the strength of a cylindrical field-magnet.

Take the instant at which the current in phase A is at its maximum and going away from the observer in the six slots at the top of the armature in Fig. 300. The current in phases B and C will then be at one-half their maximum value, so the ampere-turns of the armature tending to drive flux along a horizontal diameter will be

$$1{\cdot}41 \times I_a \times \frac{Z_a}{2p} \times \frac{2}{3} = \frac{0{\cdot}47 I_a Z_a}{p}.$$

After one-twelfth of a period has elapsed, the current in phase A will have sunk to 0·866 of its maximum value, and the current in phase C will have risen to

0·866 of its maximum value, while phase B will be at zero. At this instant the ampere-turns on the armature will be

$$0{\cdot}866 \times 1{\cdot}41 \times I_a \times \frac{Z_a}{2p} \times \frac{2}{3} = \frac{0{\cdot}407 I_a Z_a}{p}.$$

Observe that we are only considering the magnetomotive force along the centre-line of the magnetic path. The field form of these cylindrical magnets is so nearly sinusoidal and the effect of the armature reaction is so close to what would be produced by a sinusoidal distribution * of current that we find it sufficient to consider only the crest values of the magnetomotive force (see page 396).

The **mean value of the ampere-turns of a three-phase armature** is therefore

$$0{\cdot}437 I_a Z_a,$$

where I_a is the virtual current per conductor and Z_a the total number of conductors on the armature.

Let us represent these ampere-turns by a vector I_{za} drawn from O and pointing to the centre of the phase band A. The vector points in the direction in which the current flows along the connectors from the bottom to the top of the armature. The flux produced by these ampere-turns if acting alone would be along a horizontal diameter, but it will be found more convenient to draw the ampere-turn vector pointing to the centre of the phase band A than to draw it along the line of the flux. In the same way, the ampere-turns of the field-magnet can be represented by a vector I_{zf} drawn parallel to the direction of the current in the end connectors of the rotor. If we add together the vectors I_{zf} and I_{za}, we get the resultant vector I_{zr}, which gives the actual magnetomotive force on the magnetic circuit of the generator. This will create the actual working flux along a line at right angles to I_{zr}. The centre of this flux will be on the line of the vector E_g, and if we know the magnetization characteristic, we can draw the vector E_g to the volt scale to represent the phase and amount of the generated E.M.F. Here we have drawn the vector representing the E.M.F., so that it points to the phase band in which the E.M.F. is at its maximum. It will be seen that the vector summation of I_{zf} and I_{za} has taken into account both the demagnetization and the cross-magnetization effect of the armature. If we know the voltage drop in the armature due to its self-induction we can set off the reactance voltage by the vector $I_a x_a$ and the drop in the armature resistance by the vector $I_a r_a$, and thus we arrive at the terminal E.M.F. E_t. This is, of course, made up of the ohmic drop $I_a R$ in phase with the armature current and the inductive drop, in the outside circuit, represented by the horizontal vector $I_a X$.

Now, there are two ways in which this diagram may be arrived at. (1) By calculation from the data of the machine and the outside circuit, and (2) from experiments on the machine at no load and deductions from the results obtained.

First let us see how we can draw the diagram from calculated data. We must calculate the no-load magnetization curve (sometimes called "the saturation curve") of the machine, that is, the curve connecting ampere-turns per pole on the

* See article by Dr. S. P. Smith and W. H. Barling, *Electrician*, October 16, 1914, for proof that the pointed and flat-topped M.M.F. distributions have the same fundamental sine wave, and that this may be taken to represent the curve of mean distribution of M.M.F.

field-magnet with the flux per pole, or the maximum flux-density in the air-gap. Such a curve is given in Fig. 301. The method of obtaining it is given on page 321. According to our method of calculation, it is most convenient to make the ordinates represent flux-density in the air-gap. The voltage generated is proportional to the flux-density in the air-gap. Next, we must draw a curve such as that drawn with the dotted line in Fig. 301, which shows the increase in the ampere-turns required to drive the working and leakage flux through the magnetic circuit when

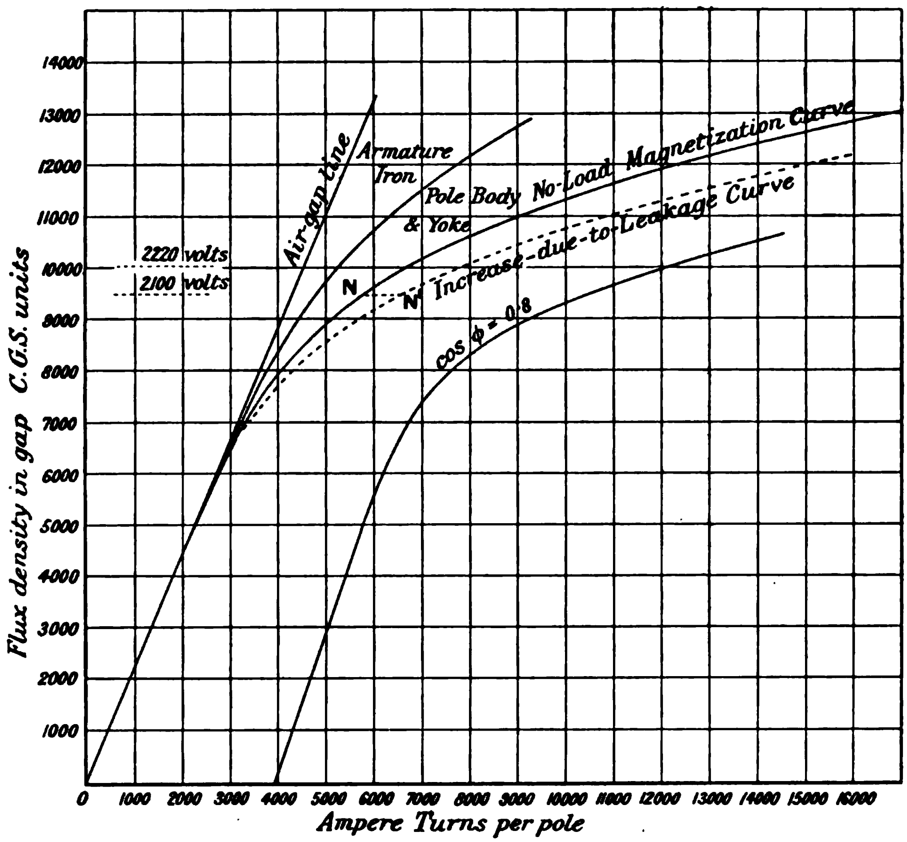


FIG. 301.—Magnetization curve of 750 K.V.A. generator.

we have the increased pole leakage that occurs at full load. For instance, in Fig. 301 at no load the ampere-turns required to drive the full-voltage flux, that is, to give 9500 C.G.S. lines per sq. cm. in the gap, is 5760. Now, it is shown later that at full load 0·8 power factor (that being the specified power factor) the extra ampere-turns absorbed on the field iron due to increased leakage is 800. Draw the horizontal line NN' to scale to represent 800 ampere-turns from the point N on the no-load magnetization curve, and thus get the point N^1 on the dotted curve. Thus a new curve can be drawn, giving the increase in the ampere-turns due to extra leakage on load.

We will now suppose that the no-load magnetization curve and the increase-due-to-leakage curve have been drawn, and that it is required to obtain the relation between the ampere-turns on the field-magnet and the voltage when full-rated current is flowing in the armature at a specified power factor, say 0·8. First calculate the armature ampere-turns per pole from the formula:

$$\frac{0{\cdot}437\, I_a Z_a}{\text{No. of poles}} = I_{za} \text{ per pole.}$$

Set this off as a vector to scale * as in Fig. 300. The quantities in this figure are taken from the example worked out on page 321. This vector also represents the armature current to another scale. Next set off $E_t = 2100$ volts, the terminal voltage at the correct angle ϕ according to the power factor of the load. In this case ϕ will be such that $\cos\phi = 0{\cdot}8$. The armature resistance is usually so small in comparison with the self-induction that we may usually for this purpose neglect the armature ohmic drop, but in Fig. 300 it is shown by the vector $I_a r_a$. For most commercial calculations it is sufficient to guess at the armature reactance voltage from a general knowledge of the design. It is usually between 5 and 10 per cent. of the generated voltage. If it is required to work it out more exactly, this can be done by the method described on page 388. In the present case the reactance voltage, 8 % of full voltage, is set off by the vector $I_a x_a$, and thus we get the vector E_g, representing 2220, the generated voltage. Referring now to the increased leakage curve in Fig. 301, we find that to generate a voltage of 2220 volts, we require an excitation of 7700 ampere-turns per pole. Set off the vector I_{zr} to represent 7700 ampere-turns at right angles to E_g. The field ampere-turns per pole are then represented to scale by the vector I_{zf}, which is obtained by subtracting I_{za} from I_{zr}.

The direction of the resultant field lies along E_g. This is in general not the same as the mechanical centre-line of the poles shown by the dotted line NS. The armature ampere-turns I_{za} operate partly as a cross-magnetizing M.M.F., distorting the crest of the field form to one side of the centre line, and partly as a demagnetizing M.M.F., weakening the effect of I_{zf} the applied ampere-turns. The angle ψ between the current vector and the centre-line of the poles is sometimes spoken of as the "internal displacement angle." The resolution of I_{za} into its two components I_{zc} and I_{zd} is considered later in Fig. 314.

If we replace the curves which represent the distribution of the M.M.F.'s along the air-gap by their equivalent sine-wave distributions, we should get a diagram like that shown in Fig. 302,† in which F_1 represents the distribution of M.M.F. due to I_{zf}, and F_2 represents the M.M.F. distribution due to the armature ampere-turns. F_2 is shown resolved into two components F_q, the cross-magnetizing effect, and F_g, the demagnetizing effect. The angle ψ shows the displacement of the centre of the current phase-band behind the mechanical centre-line of the pole.

Suppose now that the machine is run with the armature short circuited, and that the field current is brought up to such a value that the armature just carries full-load current. The voltage at the terminals, E_t, will be zero, and the voltage

* On p. 280 we did not divide by the number of poles. There we took ampere-turns on two poles both for the armature and field.

† *Allgemeine Elektrotechnik*, Bd. iii.

generated will then be E_i, the voltage required to drive the current through the impedance of the armature. Some flux, though very little, is required to generate E_i. Let the resultant ampere-turns required to drive this flux be represented by the vector I_{zi}. In order to get this resultant, it is of course necessary to more than overcome the armature ampere-turns I_{za}. We see, therefore, that the field ampere-turns required to drive full-load armature current on short circuit is represented by a vector I_{zs}, which is greater than I_{za} by an amount I_{zi} depending on the value of the armature impedance. If the iron of the machine were not saturated the extra ampere-turns I_{zi} required on account of the impedance of the armature would be the same at all voltages, and we would have a very simple construction for finding the ampere-turns on the field at full load from the ampere-turns required on short circuit and no-load magnetization curve. We will neglect the effect of the armature resistance for the sake of simplicity, though it can be

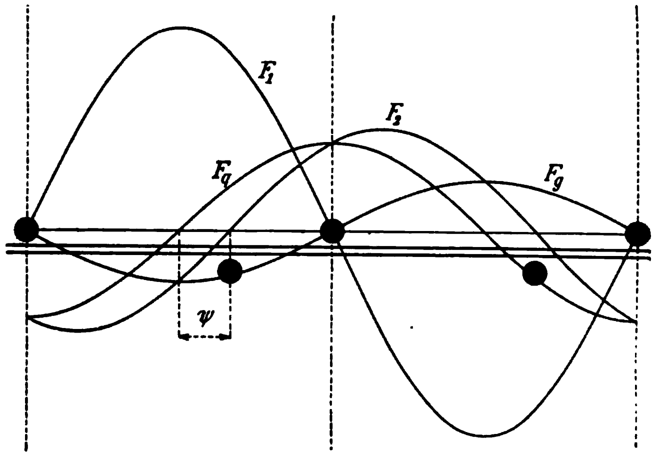


FIG. 302.—The summation of armature and field magneto-motive forces.

taken into account in the construction if we wish it. In Fig. 304 set off I_{zs} to represent the ampere-turns on the field-magnet on short circuit. This is of two parts, I_{za} the true armature ampere-turns, and I_{zi} the ampere-turns required to drive the flux which generates the E.M.F. that overcomes the armature impedance. Let E_t represent the terminal voltage, ϕ being the angle of lag, and E_g the generated voltage. At right angles to E_t set off I_{zt}, the ampere-turns required to give the voltage E_t at no load, and set off I_{zs} downwards from the end of I_{zt}. Then I_{zf} gives us the ampere-turns at full load. The relation of this figure to Fig. 300 is seen if we insert the vector I_{zr}. In a word, we have set off I_{zt} at right angles to E_t and added I_{zs} instead of setting off I_{zr} at right angles to E_g and adding I_{za}. The advantage is that I_{zt} is obtained at once from the no-load magnetization curve of the machine, and I_{zs} is obtained from the short-circuit test. Thus the triangle $I_{zt}I_{zs}I_{zf}$ is all that is required to find the full-load ampere-turns, *if we can neglect the saturation of the iron* and the resistance of the armature. The correction for the resistance of the armature is obvious

from Fig. 300. It merely has the effect of throwing I_{zi} out of line with I_{za} (see Fig. 303). The correction for the saturation as given in Fig. 301 is twofold. In the first place, E_g being greater than E_t will, if the field is saturated, call for an increase in the exciting current greater than I_{zr}. In the second place, the ampere-turns on full load cause much more leakage than at no load, and the leakage flux causing extra saturation again calls for more ampere-turns. For

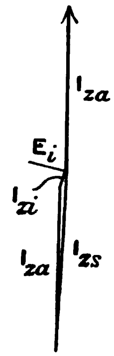


FIG. 303.—Vector diagram of a short-circuited generator.

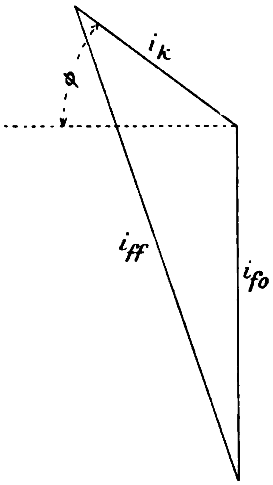


FIG. 305.—Simplified construction for finding full-load ampere-turns.

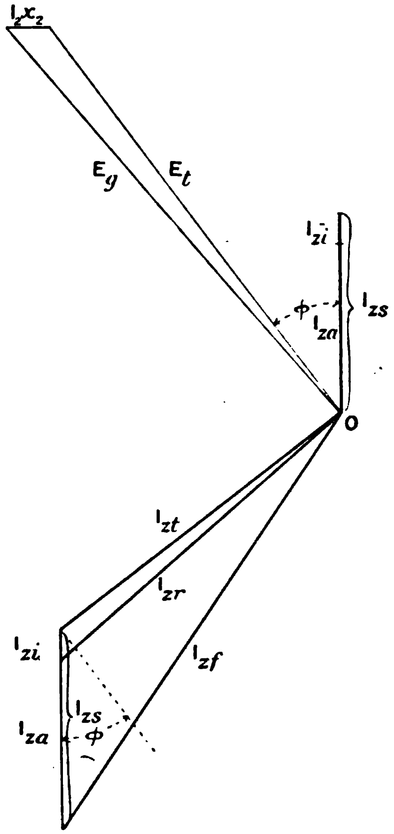


FIG. 304.—Showing the construction for finding the ampere-turns per pole for a load of any power factor.

approximate calculations, however, these effects are often neglected, and the simple triangle of Fig. 305 is used in conjunction with the magnetization curve of the machine in the manner described below.

This then brings us to the method of finding the exciting current at full load by means of data obtained from measurements made at no load. By running the machine at normal speed at various excitations, and measuring the voltage generated, we obtain the no-load characteristic. The curve marked E in Fig. 306 is such a curve, relating to a 4-pole, 2000 K.V.A. 5000 volt three-phase generator of 50

periods, whose full-load current per phase is 232 amperes. By running the machine with the armature short-circuited through ampere-meters, and measuring the field current for various values of the armature current, we obtain the short-circuit characteristic such as that marked i_k.

We have seen from Fig. 303 that the field-current on short circuit is made up of two parts. One part supplies the ampere-turns necessary to overcome the armature ampere-turns I_{za}, and the other supplies the ampere-turns I_{zi} necessary to give sufficient flux to generate the voltage that drives the current through the impedance of the armature. None of the experimental methods that have been

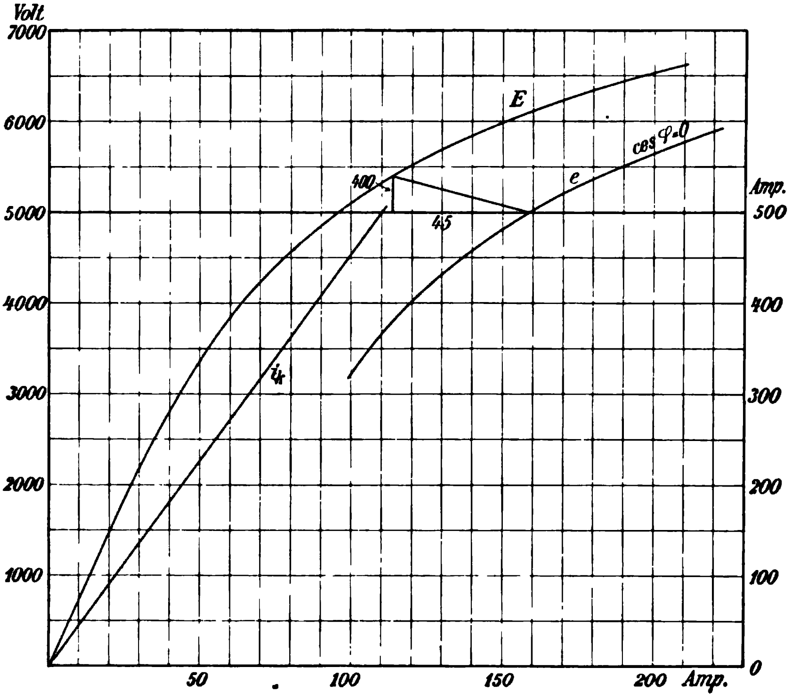


FIG. 306.—No-load and short-circuit characteristics of a 2000 K.V.A. 5000 volt 3-phase generator.

proposed for separating the short-circuit current into these two components are satisfactory in practice. It is usual to deal with the short-circuit current in its entirety as shown in Fig. 305. The ampere-turns required to give full voltage at no load is represented by a vector i_{fo} as in Fig. 305, and the short-circuit current. i_k is laid off at the correct angle as shown, where ϕ is the angle of lag. Then i_{ff} would be the full-load field current, if there were no saturation. The triangle in Fig. 305 is simply a copy of the triangle $I_{zt}I_{zs}I_{zf}$ in Fig. 304 to a different scale. For many purposes this simple method is sufficient, especially where our knowledge of the machine in question enables us to make a mental correction for the increase in the current due to saturation. It will be found sometimes that throughout a whole line of standard machines the ohmic drop in the armature is about the same percentage of the normal voltage, and is approximately known. Similarly,

the reactance drop in the armature is usually known approximately, and where this is so the part I_{zi} can be calculated and subtracted from the short-circuit field ampere-turns, leaving I_{za}, from which the field amperes required to overcome the armature demagnetizing effect can be calculated.

When the amount of the impedance of the armature winding is known, a skeleton diagram such as that shown on Fig. 307 will be found useful where it is required to find the field-currents at various power factors. Copies of this skeleton diagram can be kept at hand, with the radial lines at both ends already drawn in position. The radial lines on the left-hand side are for setting off the armature ampere-turns, and those on the right for setting off the armature impedance drop. The method of using this diagram will be best understood by working out an example.

Suppose that in the machine to which Fig. 306 refers, the reactance voltage of the armature with full-load current flowing is 8 % of the normal voltage, and that the ohmic drop in the armature is 1 % of the normal voltage. The horizontal line OE (which may be conveniently 100 units long) stands for unity or full normal voltage. The small semi-circles on the right-hand side mark off 5 % and 10 % reactive drop respectively from the little radial lines. Suppose that we wish to find the exciting current of the machine in question when operating at full load, 0·8 power factor lagging. We mark off 8 % reactive drop on the little radial line marked 0·8 power factor lagging, and then set off the ohmic drop tangentially, and arrive at the point $E_{\cdot 8}$. Then the line $OE_{\cdot 8}$ is proportional to the generated voltage, $=5300$, at the load in question. Referring now to the magnetizing characteristic (Fig. 306), we find the field current required for 5300 volts. This is 109. Set off 109 amperes along the voltage line to any convenient scale. The field amperes on short-circuit (armature current 232 amperes) are 52. We can divide this into two parts just as I_{zs} was divided into two parts, I_{zi} and I_{za}. The part required to generate the 400 reactive volts is (from Fig. 206) 7 amperes, so that the true demagnetizing part is 45 amperes. Set off the 45 amperes to scale, along the left-hand radial line which corresponds to a power factor of 0·8 lagging. Completing the triangle, we find that the full-load field current is 141 amperes. And so for any other power factor. For $\cos\phi=0$, we see that by the construction we merely add the 8 % or 400 volts to the 5000 volts, and find from Fig. 306 the corresponding field current, and then add the 45 amperes directly to it. This is shown in Fig. 306, and the characteristic for full load $\cos\phi=0$ is there plotted for various voltages.

To find the field current at half load the same construction is adopted, except that the impedance drop is taken at half the value and only 22·5 amperes are laid off along the radial line instead of 45 amperes. And so for any load.

Having obtained the field current at any load, it is easy to find the rise in voltage which takes place when that load is thrown off, by finding from the no-load characteristic the voltage that would be generated by the increased field current and subtracting from it the normal voltage. If we plot the percentage of normal voltage obtained when various loads at various power factors are thrown off, we get curves like those given in Fig. 307a for $\cos\phi=1$ and $\cos\phi=0{\cdot}8$, which have been calculated from the data given in Fig. 306.

FIG. 307.—Skeleton diagram for working out rapidly the excitation at any load and any power factor

It is of interest to enquire how the field current varies with the power factor when the armature is carrying full-load current. It will be seen from Fig. 308

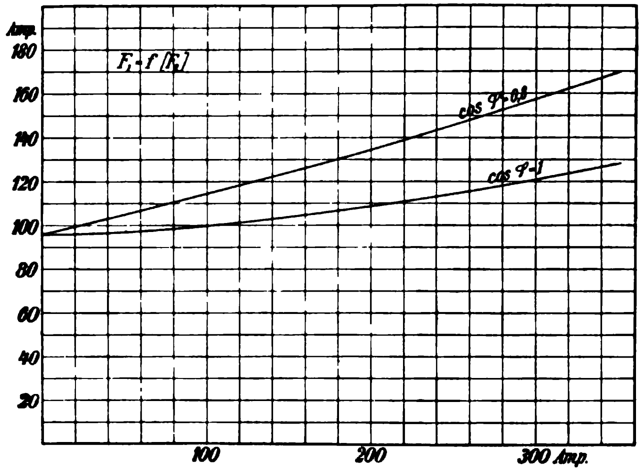


FIG. 307*a*.—Showing rise of voltage with load thrown off.

that for small changes in the power factor the field current changes quite considerably. There is very much more change in the field current in changing the power factor from unity to 0·95 than in changing it from 0·95 to 0·9; as the power factor

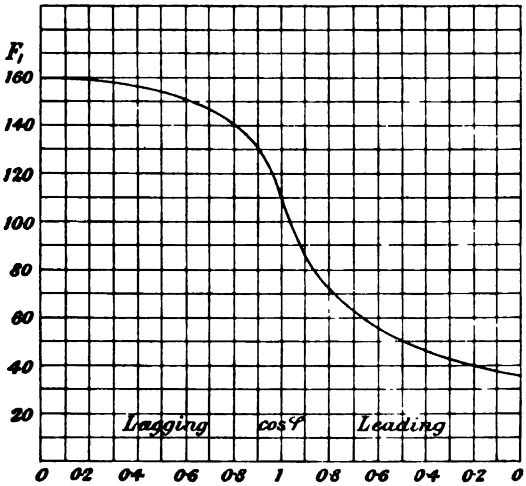


FIG. 308.—Curve showing relation between exciting current at full load and the power factor of the load.

gets lower the effect of changing it becomes less, until at $\cos\phi = 0$ the rate of change becomes zero.

The curve showing the relation between the change in the voltage with the change in the power factor, on a machine having as much saturation as that shown

in Fig. 306, and having considerable ohmic drop in the armature, differs in shape from the curve connecting field current with power factor. This will be seen from Fig. 309, which has been plotted from the data of the machine to which Fig. 306

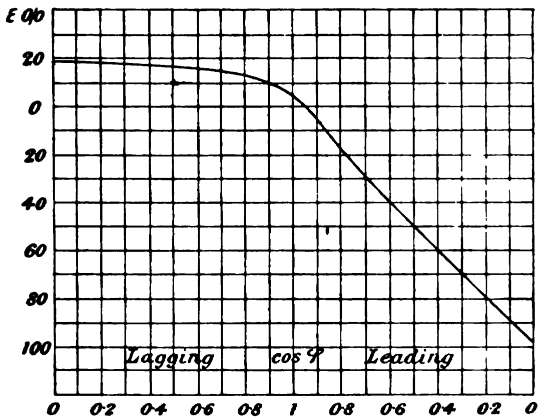


FIG. 309.—Curve showing relation between the change of pressure when load is thrown off and the power factor of the load.

refers. It will be noticed that for leading power factors the two curves are nearly the same shape, but at lagging power factors the effect of the saturation and the ohmic drop is to make the curve in Fig. 309 almost straight.

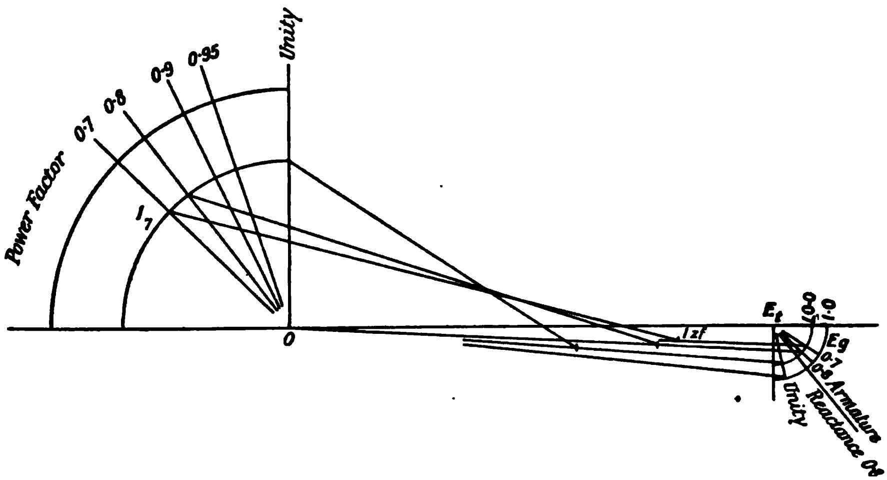


FIG. 310.—Field-current diagram for 750 K.V.A. generator.

Strictly speaking, the magnetization curve to be used to find the field current on load from the generated voltage should be the curve corrected for increase-due-to-leakage, as shown in Fig. 301. But in practice one commonly makes a mental correction for this.

In Fig. 310 is given the skeleton diagram worked out for the power factors, 1·0, 0·8 and 0·7, from the data relating to the 750 K.V.A. machine calculated

on page 321, whose magnetization curve is given in Fig. 301. In this case the increase-due-to-leakage curve has been employed (see page 330). By means of the construction given in Fig. 310 it is possible to plot curves which show the change in voltage which occurs when any load at any power factor is thrown on. Such a series of curves are given in Fig. 311.

In practice, however, regulation curves are not so much required to show what any particular machine will do, as to tell the designer what frame he must employ in order to meet a given regulation guarantee. For this purpose curves of the type

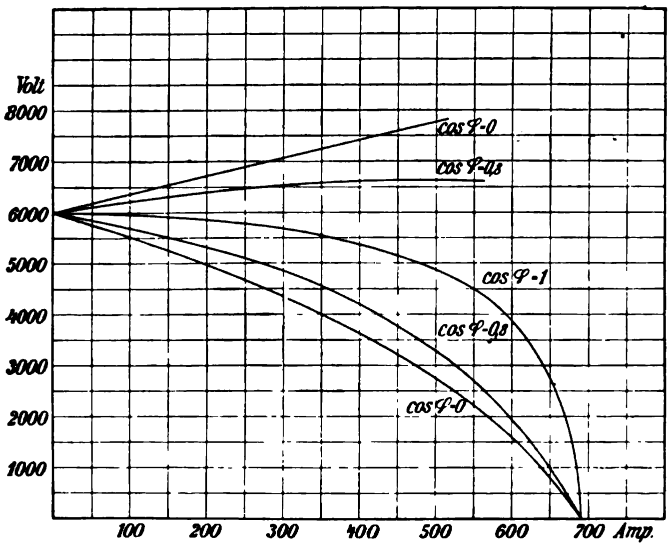


FIG. 311.—Showing how the pressure of a 6000-volt generator varies when loads of various power factors are thrown on. The two upper curves relate to leading power factors.

shown in Fig. 312 are of great service, because they relate not merely to one machine, but to any polyphase generator. We take for abscissae in this figure the ratio $\frac{\text{armature A.T.}}{\text{field A.T. on no load}}$, because upon this ratio the regulating quality of the machine depends. By "armature A.T." for any given armature current we mean (in this connection) the number of ampere-turns that must be put upon the field-magnet in order to make the given armature current flow through a short-circuited armature. The curves are obtained by drawing a number of triangles, such as shown in Fig. 305 with various ratios between i_K and i_{fo}. The abscissae give the ratio $\frac{i_K}{i_{fo}}$ and the ordinates marked "percentage regulation up" give the percentage by which i_{ff} is greater than i_{fo} for various power factors. If, now, we take various ratios between i_K and i_{ff}, and find the percentage by which i_{fo} is less than i_{ff}, we find the "percentage regulation down" plotted in Fig. 312 for various power factors. These curves give the regulation as it would be on an unsaturated generator. Where we have the no-load magnetization curve of any machine given, the regulation on that machine can be ascertained by taking the change in field

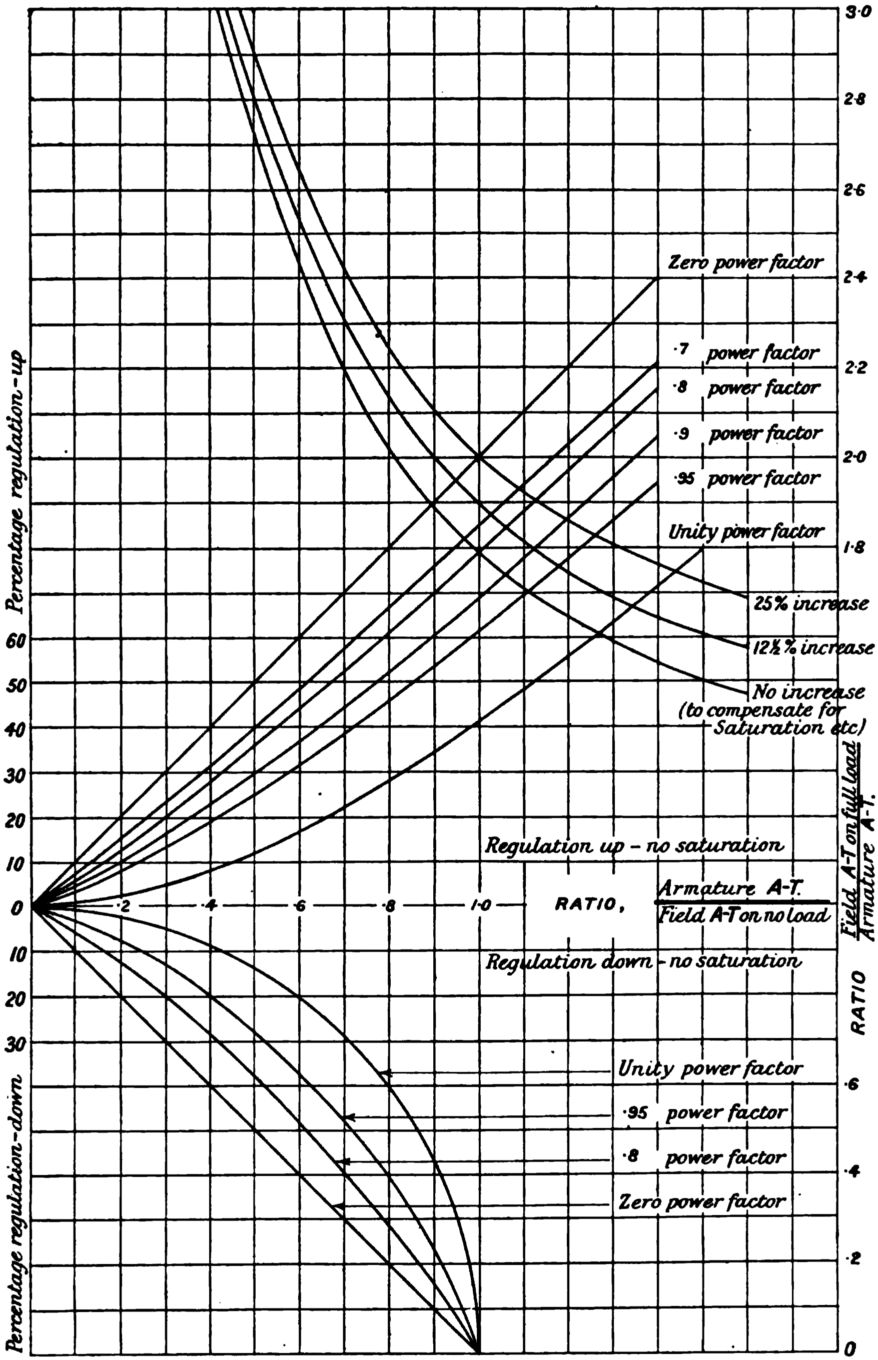


FIG. 312.—Curves for finding rapidly the regulation of any generator at any power factor or for finding the ratio of armature A.T. to field A.T. for any required regulation.

current from Fig. 312, and finding from the magnetization curve the corresponding change in the voltage.

A few examples will clearly illustrate the method of using these curves.

EXAMPLE 43. It is required to find the rise in voltage which will occur on the machine referred to in Fig. 306 when 2000 K.V.A., at power factor 0·8, is thrown off. From the curve i_K we see that a current of 232 amperes in the armature on short circuit requires 52 field amperes. From curve E, 5000 volts at no load require 96 field amperes. Therefore the ratio armature A.T. to field A.T.,

$$\frac{52}{96}=0{\cdot}54.$$

From the abscissa 0·54 in Fig. 312 run up to the 0·8 power factor curve, and we find the increase in excitation is given as 40 %. This would give us 135 field amperes, assuming no saturation, and according to that the voltage would rise to 5800 volts on the load being thrown off. The more complete construction given in Fig. 307 gives us 141 amperes for the exciting current, and the voltage would rise to 5860 volts. For field-magnets of the cylindrical type, the use of the curves in Fig. 312 does not give as accurate results as the construction given in Fig. 307. We shall see later that for machines with salient poles the method given in Fig. 312 gives exciting currents which are rather too high. That is to say, the figures obtained are on the safe side (see page 365, where a case is worked out by three different methods).

EXAMPLE 44. The 2180 K.V.A. generator referred to on page 348 is running at no load with an excitation of 96 amperes. A load of 116 amperes (half load) at a power factor of 0·95 is suddenly switched on. How much will the voltage drop, assuming that the speed and excitation remain constant?

As before, we find the ratio of armature ampere-turns to field ampere-turns

$$\frac{26}{96}=0{\cdot}275.$$

From the abscissa 0·275 in Fig. 312 we drop a perpendicular to the 0·95 power-factor curve (regulation down), and we find that the armature reaction weakens the excitation by 11 %. Referring now to page 348, we see that an excitation of 85 amperes gives a voltage of 4700.

The fixing upon a frame for the building of a generator often depends upon the maximum number of ampere-turns that the frame can carry without over-heating. It is therefore necessary that we should be able, when given the maximum field ampere-turns that a frame will carry, to say what regulation that frame will give when the armature is loaded at a definite current loading.

The curves in the upper part of Fig. 312 enable us to do this quickly for the case where $\cos\phi=0{\cdot}8$, and with sufficient accuracy for the purpose of choosing the size of frame. An example will make the matter clear.

EXAMPLE 45. Suppose that we have a 16-pole frame that will carry a maximum of 10,000 ampere-turns per pole, or 160,000 ampere-turns total. If we put a current loading on this frame of 124,000 ampere-wires, what kind of regulation would we expect to get at 0·8 power factor? The ampere-turns on the field to give full-load current on short circuit, I_{ss} (see Fig. 303), may be taken as very nearly one-half the ampere wires, in this case 62,000 ampere-turns.

Take the ratio $$\frac{\text{field A.T. on full load}}{\text{armature A.T.}}=\frac{160{,}000}{62{,}000}=2{\cdot}6.$$

Take the ordinate 2·6 on the right-hand side of Fig. 312. Run along the horizontal line until we come to the curve marked "no increase." Then drop vertically to the abscissa scale and read off 0·54. This is the ratio of armature A.T. to field A.T. at no load. From this we can run up to the curve marked power factor 0·8, and find that the field current will have to be increased 40 % on that power factor. The actual regulation will depend upon the amount of saturation in the magnetic circuit (see Example, page 293).

Now, the curve marked "no increase" has been plotted on the assumption that there has been no increase in the field current at full load due to saturation. As this assumption is not warranted in machines in which we are relying upon the saturation to improve the regulation, the two curves marked $12\frac{1}{2}$ % and 25 % respectively have been introduced. The first of these is plotted on the assumption that the saturation of the frame is such that when the ampere-turns on the armature are made equal to the no-load ampere-turns on the field, there is an increase in the field ampere-turns on a load of 0·8 power factor of $12\frac{1}{2}$ % over the amount calculated by Fig. 305 due to the saturation of the magnetic circuit. For smaller current loadings a smaller allowance is made for saturation. This curve is the one which will generally be used with A.C. generators as commonly constructed. The 25 % curve and other curves which we can imagine to be drawn in between the two are intended to be used when we are dealing with more highly saturated machines.

EXAMPLE 46. A certain frame can carry a maximum field A.T. of 175,000. If a generator built upon it is designed so that the ampere-wires $I_aZ_a = 120{,}000$, and if the amount of saturation in the magnetic circuit is the usual amount as indicated in the characteristic curves in Fig. 306, what voltage rise will we get when full load at 0·8 power factor is thrown off?

As before, take the ratio $$\frac{175{,}000 \times 2}{120{,}000} = 2{\cdot}9.$$

Running along the line 2·9 until we come to the $12\frac{1}{2}$ % curve and then downwards, we get the abscissa 0·47 for the ratio between armature A.T. and no-load field A.T. At this ratio the increase in the field current at full load 0·8 power factor is 33 %. Referring now to the no-load characteristic, we find that an increase in the field current by 33 % will give a rise in the voltage of 14 %.

These curves are, of course, only intended for obtaining approximate results. By means of them we can save a great deal of time in preliminary calculations. We can, for instance, find out in the course of a few minutes whether it is possible to squeeze a generator of a certain output upon a certain frame and yet stand a good chance of meeting certain guarantees as to regulation. They at the same time tell us, for any particular frame, what ratio we may take between armature A.T. and no-load field A.T., and yet not exceed at full load the maximum A.T. of the field that we know must not be exceeded.

The full-load ampere-turns for the purposes of the ratio set out on the right-hand side of Fig. 312 are taken as the full-load A.T. at 0·8 power factor, that being the power factor for which machines are most commonly designed. It would be necessary to plot other curves if the ratio between armature A.T. and full-load field current at other power factors were the basis of the calculation.

The method given above for the calculation of the exciting current of a loaded generator is strictly only applicable to a machine with a cylindrical field-magnet, such as is illustrated in Fig. 371, in which the reluctance of the magnetic path is the same in every direction. Where a machine has **salient poles**, the reluctance of the path for the cross-magnetizing flux is higher than for the working flux, and therefore the vectors representing fluxes are not proportional to vectors representing magnetomotive forces. The circumstance is only of importance where it is necessary to calculate the regulations at high-power factors as accurately as possible.

For ordinary commercial purposes the method given on page 284 is commonly applied to salient pole machines, because it gives results which are sufficiently near for practical purposes, and the error that exists is on the safe side.

The more exact method of calculating the exciting current will be understood from the example worked out below. It differs from the method given on page 284 mainly in the fact that it is necessary to resolve the armature magnetizing effect into two parts, one acting directly against the field ampere-turns and the other acting at right angles to it and constituting a cross-magnetizing effect. This latter magnetomotive force must be multiplied by a coefficient, depending on the ratio of pole arc to pole pitch, before we can arrive at the effect it will have in

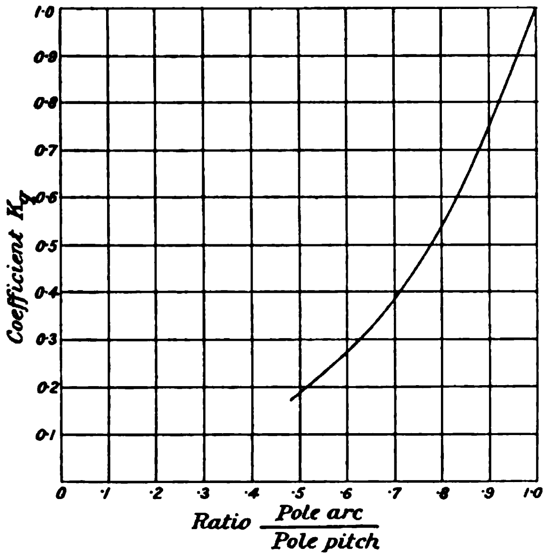


FIG. 313.

shifting the phase of the E.M.F. behind the centre line of the pole. This coefficient we denote by K_q. Fig. 313 shows how the coefficient K_q changes with the pole arc on ordinary three-phase generators having poles of the kind shown in Fig. 234.

The method is one of trial and error, because the angle ψ between the phase of the current and the centre line of the pole, " the internal displacement angle," depends upon the amount of the cross-magnetizing effect, and this again depends upon ψ.

A diagrammatic view of a generator having two salient poles is shown in Fig. 314. The arrangement of the armature conductors is the same as in Fig. 300, and we may, for the purpose of our example, take the armature ampere-turns on the full-load and no-load characteristic the same as in the example worked out on page 282.

The method of finding the field ampere-turns on full load is as follows:

Lay off the vector I_{za} to represent the full-load armature ampere-turns. This vector will point to the centre of the phase band A when the phases of the currents are as indicated in the figure. Lay off the vector E_t to represent the terminal

E.M.F., the angle ϕ being the angle of lag between current and voltage according to the power factor of the load. Then, as before, lay off $I_a r_a$ and $I_a x_a$ and so obtain the generated voltage E_g. We know that the phase of this generated voltage lags behind the phase of the centre line of the pole by an amount which depends upon the cross-magnetizing effect of the armature, and as a consequence the current lags further behind the centre line of the pole than it otherwise would. We determine the value of the angle ψ between the centre line of the pole and the current by a method of trial and error.

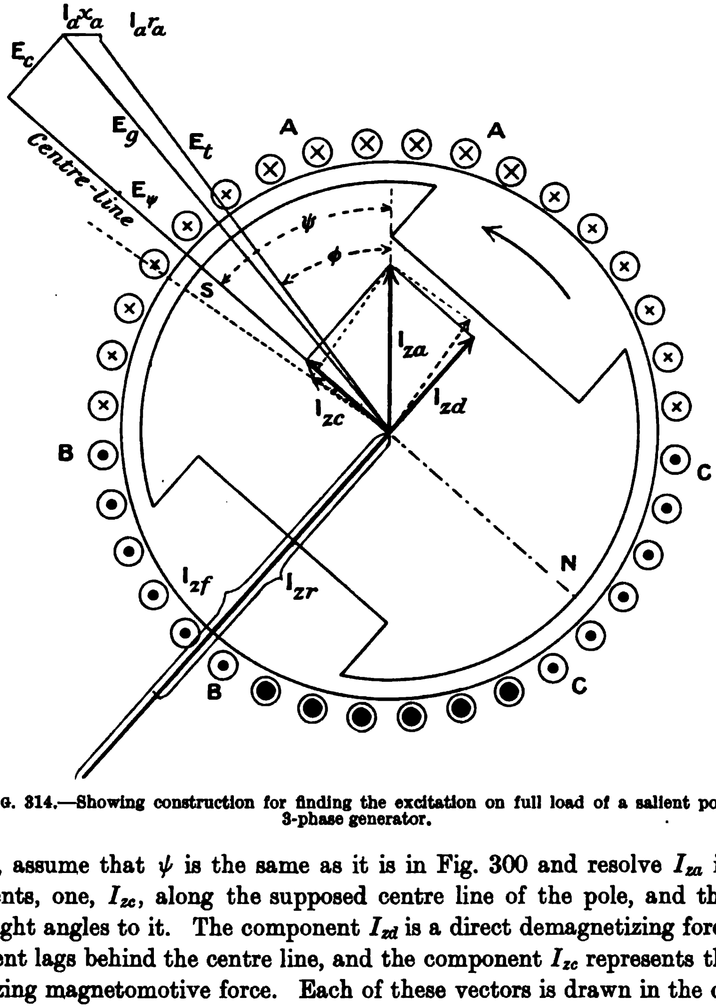


FIG. 314.—Showing construction for finding the excitation on full load of a salient pole 3-phase generator.

First, assume that ψ is the same as it is in Fig. 300 and resolve I_{za} into two components, one, I_{zc}, along the supposed centre line of the pole, and the other, I_{zd}, at right angles to it. The component I_{zd} is a direct demagnetizing force, when the current lags behind the centre line, and the component I_{zc} represents the cross-magnetizing magnetomotive force. Each of these vectors is drawn in the direction in which the corresponding current components would flow, so that the directions of the corresponding magnetizing forces are, of course, at right angles to these vectors respectively. Now, the reluctance of the magnetic path through the field-magnet at right angles to I_{zc} is greater than the reluctance of the path at right angles to I_{zd}. Hence the necessity of introducing the coefficient K_q. Scale off the

provisional I_{zc} (shown dotted) and multiply by K_q for the ratio of pole arc to pole pitch in question. In this case the pole arc is 0·65 of the pole pitch, so from Fig. 313 $K_q = 0{\cdot}32$. To the same scale as the abscissae of Fig. 301 the provisional I_{zc} represents 2000 ampere-turns. This multiplied by 0·32 is 640 ampere-turns. From Fig. 301 this would generate a cross voltage of 333. If we set off this voltage in the position shown at E_c, we will find that the provisional line taken for the centre line of the pole was not right, but the true position is very nearly indicated. A second trial gives us the true position as marked in Fig. 314. We can now resolve I_{za} into its two components, I_{zc} and I_{zd}, much more accurately. I_{zc} now represents 2300 ampere-turns; multiplying this by 0·32, we get 736 ampere-turns, which yield 380 volts according to Fig. 301. Setting off E_c to represent 380 volts at right angles to the centre line of the pole, we get E_ψ, the E.M.F. generated by the undistorted flux from the pole.

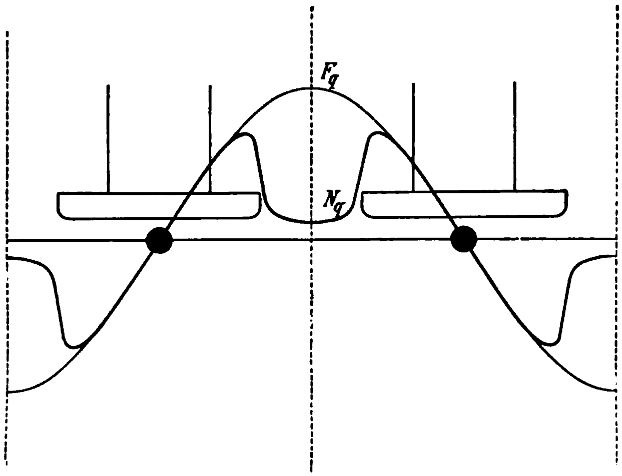


FIG. 315.—Diagram of the cross magnetomotive force F_q and the cross flux N_q on a salient pole 3-phase generator.

In Fig. 314 we have shown a two-pole machine, so that the meaning of the vectors can be made more apparent. The same construction is applicable to machines having a large number of poles. In Fig. 315 the large dots represent the centre points of the phase band of the component of the armature current, which is in phase with the centre line of the pole (compare Fig. 302). This band of current lying in a distributed winding will yield a magnetomotive distribution represented by the wave-form F_q. Taking into account the reluctance in the various parts of the magnetic circuit, we see that this distribution of magnetomotive force would produce a flux distribution, something like that indicated by the thicker curve N_q. This flux distribution, when combined with the no-load flux distribution shown at N_o in Fig. 316, gives the resultant flux distribution shown by the curve N.

It will be seen that this cross-flux distribution, if acting alone, would generate an E.M.F. (denoted here by E_c), which would have a very pronounced third harmonic, but if the armature of the generator is star connected this harmonic is entirely neutralized, and the E_c that remains is (with a pole arc 0·66 of the pole pitch)

only about one-third of what it would be if the air-gap were of the same length over the whole pole pitch (see page 308).

The no load field-form for a salient pole is shown by the curve N_0 in Fig. 316. If the armature magnetomotive distribution is as indicated by the curve F_2, the resultant field-form might be somewhat as indicated by the curve N. The exact form of this curve depends upon the amount of bevel on the poles and the state of saturation of the iron, so that without going into each case with great minuteness it is impossible to say exactly how much the field is distorted. The values given in Fig. 313 for the coefficient K_q are sufficiently near for practical purposes where the poles are of the general shape shown in Fig. 234.

For ordinary standard machines with a pole arc of about two-thirds of the pole pitch we are justified in applying the vector diagram as described in Fig. 305, notwithstanding the fact that the cross-magnetization is not wholly operative.

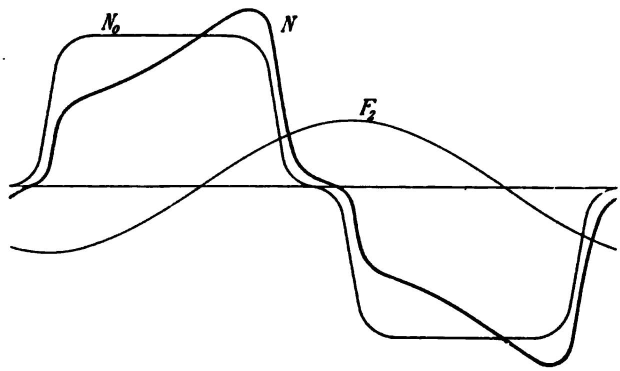


FIG. 316.—Resultant, N of main flux N_0 and cross flux N_q (Fig. 315).

It will be seen that the regulation of the machine will depend both on the ratio of the field ampere-turns to the armature ampere-turns, and on the extent of the saturation of the field system. In designing a machine to comply with any particular regulation guarantee, we may either make the ratio of field ampere-turns to the armature ampere-turns sufficiently great and have little saturation, or we may make the ratio less and have more saturation. If we wish for a generator with large overload capacity, we will adopt the first course. If we wish for a cheap machine with smaller overload capacity, we will adopt the second course, and we may adopt any intermediate course according to circumstances. If the ratio of field ampere-turns to armature ampere-turns is made too small, and an attempt is made to secure the regulation by excessive saturation, there is danger that at low-power factors it will be impossible to obtain the specified voltage at all.

It should be pointed out that the effect of the saturation in limiting the output of a machine greatly depends on the part of the magnetic circuit in which the saturation takes place. If it occurs in the *teeth of the armature only*, then it would not prevent us from obtaining full voltage at heavy loads. It is true that a little more flux is required at heavy loads, because the generated E.M.F. is greater, but even with high saturation in the armature teeth there is not a call for an excessive

number of ampere-turns to make a small increase in the total flux per pole. If, on the other hand, the great saturation occurs at the root of a rather long pole, or worse still, in the yoke itself, then it may easily happen that we cannot get even the no-load flux through the armature at heavy loads of low-power factor. The armature back ampere-turns call for more ampere-turns in the field, and these may increase the leakage to such an extent as to rob the armature of some of its flux. A further increase in the field ampere-turns causes a further increase in the leakage, and so on.

A good plan is to allow the saturation to occur on the surface of the pole. Where the mechanical construction permits of it (as, for instance, in cylindrical turbo-rotors) saturation may be produced by cutting slots near the surface of the pole. This gives a much more satisfactory magnetization characteristic than can be obtained from a simple salient pole saturated at the root. Figs. 352 and 353 show the difference between the no-load and the full-load characteristic in the two cases, the reason for the differences being as stated above. Where a punched salient pole is employed in ordinary engine-type machines, it is possible to punch slots near the pole face. This, however, requires a special die, and would not be economical unless a large number of machines (or rather a large weight of punchings) were required, using the same die. A somewhat better plan (where comparatively few machines are to be built) is that shown in Fig. 334. Here the saturation occurs just below the pole cap. This is not the best place, but it is not far removed from the best place. This plan has the advantage that the space saved by the cutting out of the iron can be utilized as copper space. The best length of pole to saturate depends on the pole pitch. With a pole pitch of 12″, a saturated length of 3″ gives good results. The extent of the saturation is a matter which requires very careful adjustment. It will be seen from the cases worked out below the sort of considerations that determine the best amount of saturation.

Before we can fix upon the size of frame upon which to build our A.C. generator, we must not only know the regulation required, but we must decide whether we are going to obtain that regulation by giving a sufficient ratio of field ampere-turns to armature ampere-turns, and relying very little on saturation, thus obtaining a machine of great overload capacity; or whether we will rely to a great extent on saturation, and build a cheaper machine, sufficiently ample to do the work it is designed for.

If we adopt the first plan, the curves in Fig. 312 are useful in arriving at ampere-turns required at full load, and in the choice of the frame upon which to begin the design. Suppose it is specified that the voltage shall not rise more than 25 % when full load at 0·8 power factor is thrown off. From Fig. 312 we see that if the ratio

$$\frac{\text{field amperes at short circuit}}{\text{field amperes at no load}} = 0{\cdot}4,$$

we will require an increase of about 28 % in the field current when on load. Now, with only the very smallest saturation, an increase of field current of 28 % will not give more than 25 % rise. So we may take the ratio 0·4 as sufficient to meet the guarantee.

Now the three-phase output of a frame in K.V.A. is equal to

$$K_e \times R_{pm} \times A_g B_k \times 10^{-9} \times I_a Z_a \times 1{\cdot}73. \quad \text{(See page 24.)}$$

$I_a Z_a$ is limited by the ampere-turns which the field-magnet is able to carry at full load without exceeding the guaranteed temperature rise. The total armature ampere-turns, as we have seen (page 280), are equal to $0{\cdot}437\ I_a Z_a$, and the short-circuit field ampere-turns may be taken roughly at $0{\cdot}47\ I_a Z_a$. Divide this by 0·4, and we get the field ampere-turns at no load; multiply by 1·28, and we get the field ampere-turns at full load. Thus we have, in this case, $1{\cdot}5 I_a Z_a$ = field ampere-turns on full load 0·8 power factor. Let the factor by which we multiply the $I_a Z_a$ to get the field ampere-turns on load be denoted by K_r. The value of K_r will depend upon the regulation required.

Then $K_r I_a Z_a = I_f S \times 2p$, where S is the number of turns per pole and $2p$ equals the number of poles, and I_f is the field current. If now we have all our frames tabulated so that we can tell at a glance what magnetic loading, $A_g B$, and what maximum number of ampere-turns, $2pI_f S$, each frame will take, it is a simple matter to fix on a frame. When we have not these data available, it is necessary to employ a $D^2 l$ formula to give us the approximate size of frame. Now as the ordinary $D^2 l$ formula takes no account of regulation, it is a good plan to modify it in the way given below. It will then be a useful guide in the choice of a frame where the regulation is specified. A 16-pole 50-cycle A.C. generator with a peripheral speed of 6000 feet per min., and with the iron and copper space well adjusted, will carry about 10,000 ampere-turns per pole for 45° C. rise. As the pole pitch is 12 inches, this amounts to 850 ampere-turns per inch of periphery. So that if we multiply $\pi D''$ by 850, we get the possible number of ampere-turns on the field of diameter D''. The ampere-turns per inch of periphery depend largely on the pitch of the poles, and on the kind of winding, as well as on the peripheral speed. Fig. 318 shows how the economical number of ampere-turns per pole changes with the diameter of the machine and the number of poles. We shall return to this matter later; for the moment we are modifying the $D^2 l$ formula to provide for the regulating qualities of the machine, and we assume that we know the figure 850 for the case under consideration.

We have the total ampere wires, $I_a Z_a = \dfrac{I_f S \times 2p}{K_r} = \dfrac{D''\pi \times 850}{K_r}$.

If we take $A_g B_k = D''\pi l \times 10$ (see p. 6), then

$$\text{output in K.V.A.} = K_e \times R_{pm} \times A_g B_k \times 10^{-9} \times I_a Z_a \times 1{\cdot}73$$

$$= \frac{K_e}{K_r} \times R_{pm} \times D^2 l \times \pi^2 \times 850 \times 10^{-8} \times 1{\cdot}73.$$

Taking K_e at 0·4, this becomes

$$\text{K.V.A.} = \frac{1}{K_r} \times D^2 l \times R_{pm} \times 850 \times 6{\cdot}85 \times 10^{-8}.$$

Thus we have the K.V.A. in terms of the diameter and length (in inches), and the regulation constant K_r. It remains to consider how the field ampere-turns per inch of periphery, which we have here taken as 850, change with the size of the machine and the number of poles. In Fig. 317 we have drawn the poles and coils in 3 cases: (1) the 8-pole case, (2) the 16-pole case and (3) the 32-pole

case, for a rotating field-magnet 60" in diameter. The length of the iron axially is supposed to be 12½ inches. As explained on page 277, the ratio of the width of the pole to the pole pitch is chosen from certain economical considerations. Where the poles are few, as in the 8-pole case, the mechanical support of the coil is also an important consideration. The ratio of the length of the pole to the pole pitch is settled by somewhat similar considerations. Where the number of poles is great, the leakage between the flanks becomes very great if the pole is made too long, so that the extra copper space gained is somewhat counterbalanced by the extra ampere-turns required to overcome the reluctance of the saturated pole base. The proportions shown in Fig. 317 may be taken as representing good economical practice.

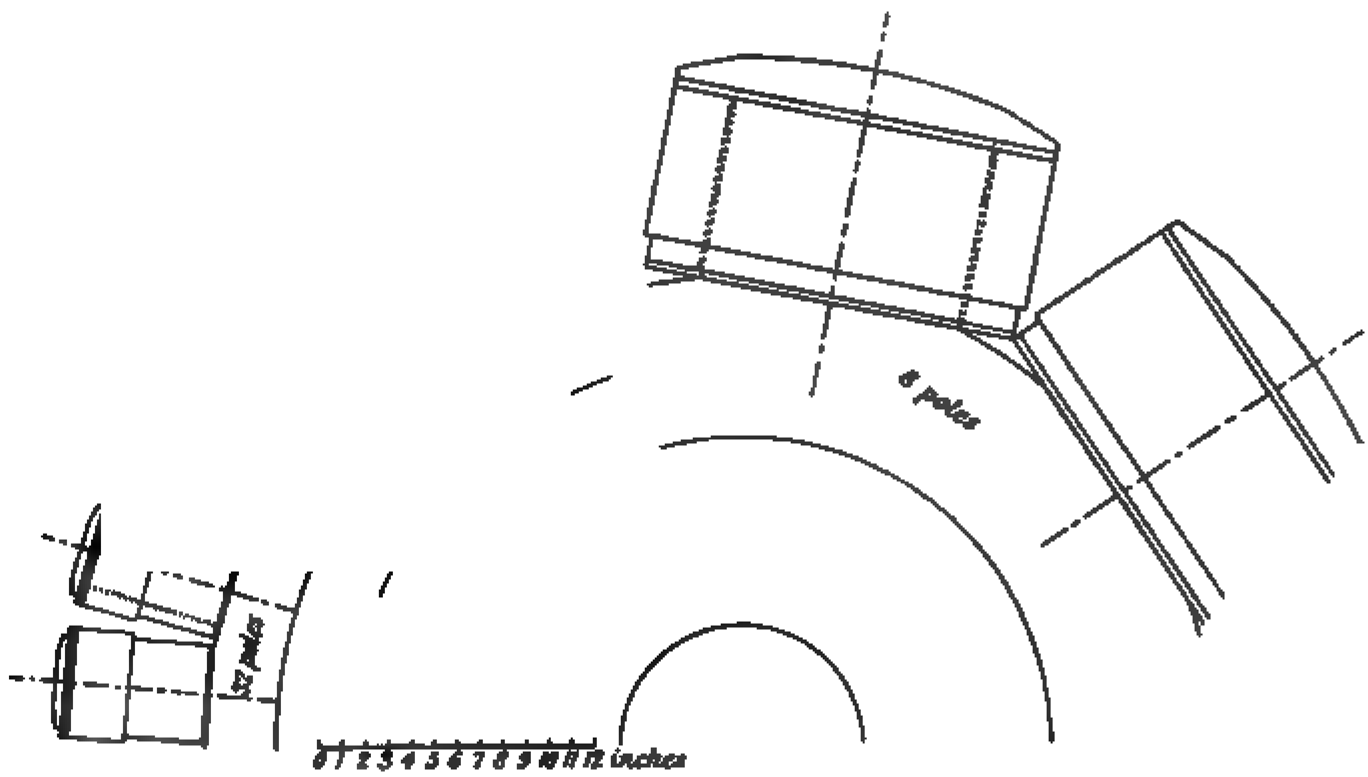


FIG. 317.—Showing arrangements of copper and iron in machines of same diameter, but with different numbers of poles.

It must be understood that it is possible to get somewhat more ampere-turns on the poles than are here considered by adopting special methods of putting on more copper, and by making ventilating ducts on the ends and sides of the coils. As the coils are subjected to great centrifugal forces, it is doubtful whether such devices are altogether to be recommended, particularly as we can quite easily increase the output of the frame by increasing its diameter. We will consider first coils wound with square double cotton-covered wire of a size suitable for excitation at 110 volts. The coils are supposed to be treated with heat-conducting enamel between layers. We have taken the case of wire-wound coils, because in general it is more difficult to treat from the heating point of view than the case of strap-wound field coils. Afterwards we will consider what modifications to make in our conclusions if strap-wound field coils are employed.

In the 16-pole case we have 118 turns of D.C.C. sq. wire 0·252 inch bare, 0·275 inch insulated. Adopting the method of calculation given on page 233, we will find that for 40° C. rise by thermometer each coil can dissipate about 554 watts. Of this, 244 watts pass through the insulation to the pole and cheeks, 250 are given

off by the ends of the coil as defined by Fig. 234, and only 60 watts by the sides, although these have an area of 2(12 × 7) sq. inches.

As the coil has a resistance when hot of ·069 ohm, calculation gives us 90 amperes as the limiting current for 40 degrees rise. 90 × 118 = 10,600 ampere-turns per pole. We will deduct 10 % from the calculated capacity, and say that we can rate the coil at 9600 ampere-turns maximum. From an actual experiment on a coil of this construction, the actual figures were as follows: Resistance of coil hot ·066. Run for 6 hours at 85 amperes. Temperature rise 34° C.

Now take the 8-pole case. This has 212 turns of the same size of wire as in the last case. Here the coil has a depth of winding of 2½ inches, so that the temperature of the surface when running will be somewhat cooler than the interior. This circumstance makes the rate of cooling per sq. in. of surface of these deep coils rather smaller than for coils of fewer layers of wire. The application of the formula given on page 233 to the coils in Fig. 317 in a method of trial and error tells us that for the 8-pole case the mean temperature of the coil is about 8° C. above the temperature of the outside, the centre being 13° C. higher than the outside. In the 16-pole case the difference is only 3° C., and in the 32-pole case less than 1° C.

We must therefore, to make a fair comparison, take the cooling of the surface in the 8-pole as if the temperature rise were only 32° C. On this basis we will find that the big coils will only dissipate about 900 watts each for 40° C. rise, and as the hot resistance is 0·167 ohm, 73 amperes therefore appears to be about the maximum field current. Allowing again a margin for safety, we may take 14,000 ampere-turns per pole as the safe rating of the 8-pole case.

A similar investigation shows that the 32-pole case can carry 95 amperes, giving 5000 ampere-turns per pole. It should be pointed out that the coil in the 32-pole case would be much better made of edgewise-wound copper strap. This would give a safe rating about 20 % higher, but for the sake of a fair comparison we have kept to square D.C.C. wire all through.

TABLE OF DATA OF REVOLVING FIELD MAGNETS.

Diameter, 60″; length, 12½″; speed, 375 R.P.M.

No. of poles	8	16	32
Turns per pole	212	118	53
Mean length of turn	60″	45½″	35″
Size of wire	0·252″ sq.	0·252 sq.	0·252 sq.
Resistance hot	0·167	0·069	0·024
Exciting current	66 amps.	81·5 amps.	95 amps.
Amps. per sq. in.	1065	1310	1530
Amp.-turns per pole	14,000	9600	5000
Total amp.-turns	112,000	154,000	160,000
Amp.-turns per in. perimeter	595	815	850
Weight of copper	2040 lbs.	1730 lbs.	1200 lbs.

The data are collected in the table above. From this table we see the great economy of material when the number of poles is increased. Though the D^2l is

the same for all machines and the speed the same, the 32-pole machine can carry 40 % more ampere-turns on the frame, notwithstanding that the weight of copper in the field-magnet is 40 % less than in the 8-pole case. We therefore cannot intelligently use any D^2l formulae or curves for finding the output of alternators, unless we take into account not only the regulating qualities of the generator, but also the effect which the frequency and speed will have upon the number of poles and the cooling conditions of the field coils.

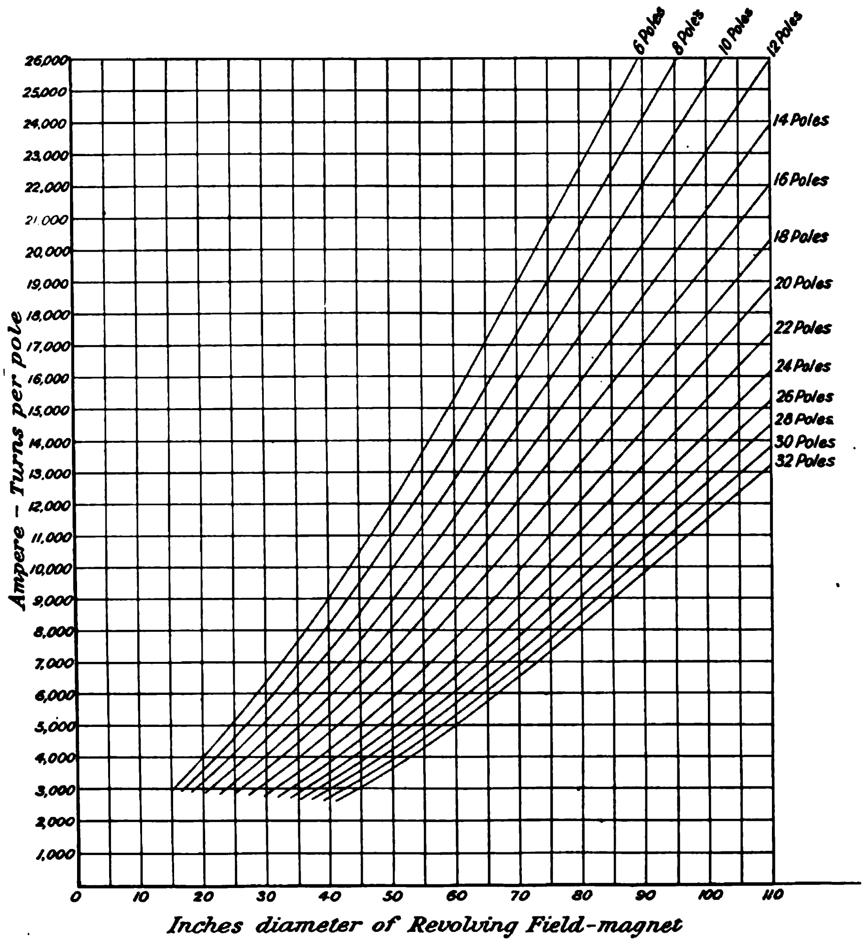


FIG. 318.

In Fig. 318 we have taken as abscissae the diameter of the field-magnet, and as ordinates the ampere-turns per pole. For 60″ diameter we have obtained three points, namely, for the 8-, 16-, and 32-pole cases. We have worked out similar cases for smaller diameters and larger diameters, so as to be able to fill in the curves as shown. The figures must not be taken as the maximum possible (see page 277), but may be taken as good economical values for the full-load ampere-turns per pole, where the coils are wound with square D.C.C. wire of a size suitable for 110

volts excitation. If copper strap edgewise-wound is employed, and the same weight used, the ratings can be increased 15 % or 20 %, but as a rule with strap field coils one saves the 15 % or 20 % of copper and keeps the rating as before. It should be pointed out that all these cases are for machine 12½ inches axial length with natural ventilation. For narrower machines the ampere-turns per pole can be slightly increased. In the 16-pole case the narrowing of the frame to 7½ inches will increase the possible ampere-turns per pole about 7 %, while for the 32-pole case the increase would be about 11 %. The 12½ inch length is an economical one for 50-cycle generators to be driven by high-speed engines. A widening of the frame will reduce the possible ampere-turns per pole.

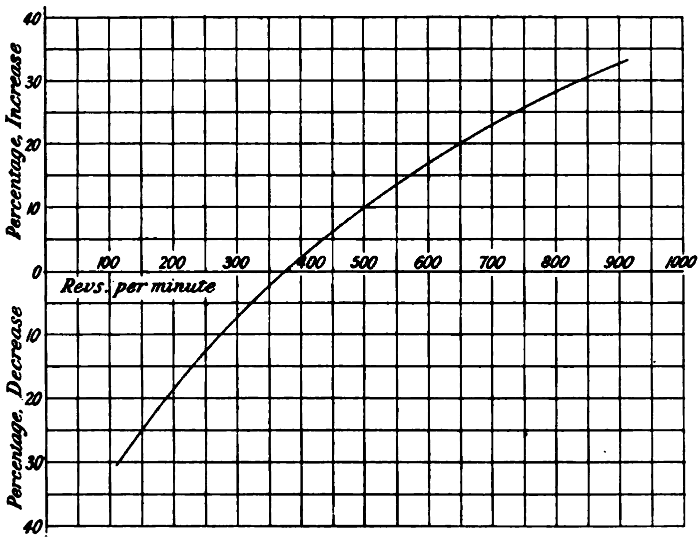


FIG. 319.—Showing percentage increase in the possible number of ampere-turns per pole as the speed of an A.C. generator is increased beyond 375 R.P.M.

Fig. 318 has been worked out for a speed of 375 R.P.M. This speed will not be suitable for some of the sizes given, so it is necessary to correct the rating for the change of speed.

We can, on the basis of the rules given on page 233, arrive at the effect of the change of speed on the possible number of ampere-turns per pole. The matter is complicated by the fact that for different frequencies the economical depth of copper is different. For machines between 25 cycles and 60 cycles, however, we may use Figs. 318 and 319 and arrive at a fair idea of the possible number of ampere-turns per pole for a field magnet of given diameter. Thus, at 60″ diameter with 16 poles, we would get a 50-cycle machine running at 375 R.P.M. with 9600 ampere-turns per pole at full load.

If this frame were used for 25 cycles running at 187·5 R.P.M. we could with the same temperature rise have 21 % less than this, or 7600 ampere-turns per pole. Or if the frame were used for 60 cycles running at 450 R.P.M. we could have 6½ % more ampere-turns, or 10,200 per pole.

If we confine our attention to 50-cycle generators, the range of peripheral speed will, for engine-type machines, be small, so that the range of ampere-turns per pole will also be small. Fig. 320, which has been deduced from Figs. 318 and 319, is useful for reference in designing 50-cycle generators. The number of poles

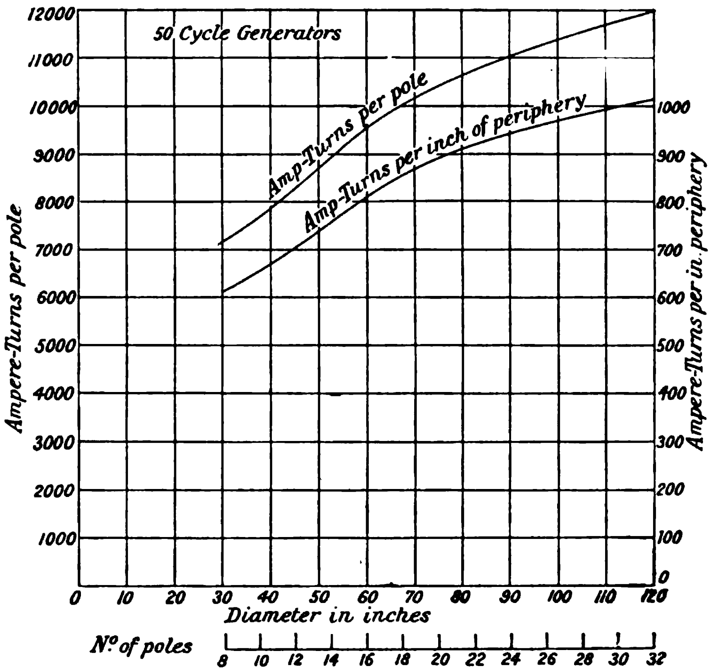


FIG. 320.—Giving the possible ampere-turns per pole for 50-cycle generators of different diameters.

given on the lower scale is the number which will be found most commonly used for frames of the diameter given. The curve giving ampere-turns per inch of periphery is also useful in finding the possible output of a frame.

THE WAVE-FORM OF THE ELECTROMOTIVE FORCE.

Where a generator is provided with open slots on the armature or on the field-magnet, these sometimes produce ripples in the wave-form of the E.M.F. It is important that a designer should be able to say in what cases ripples will be produced, and he should be able to calculate approximately the size and frequency of the ripples.

If the field-form of the magnet is a simple sine wave without higher harmonics, and if it moves forward at a constant velocity, the E.M.F. generated in each conductor must be a simple sine wave; and however many of these are connected in series, and whatever the difference of phases may be, the resultant must be a simple sine wave.

Where the field-form contains higher harmonics (see page 22), the occurrence

of these harmonics in the resultant E.M.F. depends upon a number of factors which are considered below.

Slots and projections on the field-magnet, such as pole-tips, may be regarded as the origin of the harmonics in the field-form. Slots on the armature may or may not give occasion for these harmonics to be impressed on the E.M.F. wave-form.

If a slot is skewed (see Fig. 533) by a whole slot pitch, its position is so distributed over the whole slot pitch that a winding lying in it may be regarded as a perfectly distributed winding, and the slot and tooth effect is completely eliminated. Similarly, where a machine has many poles, and the slots under one pole take up positions different from those of the slots under another pole (as in the case where there is a fractional number of slots per pole), the effect upon conductors lying in the slots and connected in series is the same as if there were a greater number of slots under the same pole taking up all the positions found under all the poles. Thus, with comparatively few slots per pole, we may get the effect of a winding distributed in a very large number of slots, and the resultant E.M.F. will be free from spacing ripples * (see Class B, pages 102 and 109).

Where, as is often found in practice, there are comparatively few slots per pole, and each pole occupies the same position with respect to the slots under it, the ripples in the E.M.F. wave-form produced by such an arrangement may be of considerable importance.

Dr. S. P. Smith and Mr. R. S. H. Boulding have made a very complete study of the ripples occurring in the wave-form of alternating-current machines, and they have kindly permitted the author to give the following abstract of a paper* which they have read before the Institution of Electrical Engineers.

A statement of the subject to be sufficiently comprehensive necessarily involves an introduction of the winding factors (see page 33) belonging to the various harmonics in the field-form.

There are two ways in which the flux embraced by a coil may vary—either the coil may move with respect to a steady flux, or the flux may change in amount with respect to a stationary

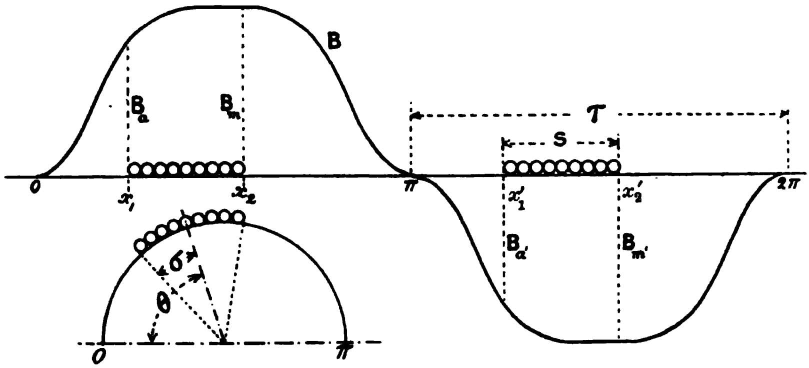


FIG. 321.—Showing a phase-band of conductors of width S moving in magnetic field.

coil. The general case occurs when these two modes of variation happen together. When a coil (Fig. 321) moves relatively to the flux, an E.M.F. of *motion or rotation* is induced in it;

* *Journ. Inst. Elec. Engnrs.*, vol. 53, p. 205, 1915.

whilst if the flux varies, an E.M.F. of *pulsation* is induced. Expressed mathematically, these two ways in which the flux ϕ may vary are given by:

$$\frac{d\phi}{dt}=\frac{\partial\phi}{\partial x}\cdot\frac{dx}{dt}+\frac{\partial\phi}{\partial t},$$

where the first term on the right-hand side denotes the change of interlinkages due to motion and the second due to pulsation.

In a heteropolar machine, the flux interlinking a coil at any instant is (see Fig. 321):

$$\phi=\int_x^{x'}\mathsf{B}l\,dx,$$

where l = core-length over which the flux-density B extends. Hence the instantaneous pressure induced in T_c turns moving at a constant velocity $v=dx/dt$ cm. per sec. with respect to the flux ϕ will be:

$$e=-T_c\frac{d\phi}{dt}10^{-8}=-T_c.10^{-8}\left(\frac{\partial\phi}{\partial x}\cdot\frac{dx}{dt}+\frac{\partial\phi}{\partial t}\right)\text{.......................................}(1)$$

$$=-T_c.10^{-8}\left\{v\frac{\partial}{\partial x}\int_x^{x'}\mathsf{B}l\,dx+\frac{\partial}{\partial t}\int_x^{x'}\mathsf{B}l\,dx\right\}$$

$$=-T_c.v.l10^{-8}\int_x^{x'}\frac{\partial\mathsf{B}}{\partial x}dx-T_c.l.10^{-8}\int_x^{x'}\frac{\partial\mathsf{B}}{\partial t}dx$$

$$=-T_c.v.l(\mathsf{B}_x-\mathsf{B}_{x'})10^{-8}-T_c.l10^{-8}\int_x^{x'}\frac{\partial\mathsf{B}}{\partial t}dx\text{ volts, ...}(2)$$

where $\int\frac{\partial\mathsf{B}}{\partial x}dx=\int d\mathsf{B}$.

With continuous-current excitation, the flux is steady, except in so far as pulsations are set up by the teeth or by armature reaction. We are here considering only the effects at no load. The effect of flux pulsations due to the teeth are considered on page 313.

When the flux is *steady*, that is, constant in value and fixed with respect to the poles, the flux-density B at any point in the air-gap is constant, so that $\frac{\partial\mathsf{B}}{\partial t}=0$, and the E.M.F. is induced by *rotation* alone. We then have

$$e=T_c.v.l(\mathsf{B}_x-\mathsf{B}_{x'})10^{-8}\text{ volts. ....}(3)$$

If the coil spans a full pole-pitch, $\mathsf{B}_x=-\mathsf{B}_{x'}$, and Eq. 3 becomes:

$$e=2.T_c.v.l.\mathsf{B}_x.10^{-8}\text{ volts. ....}(3a)$$

Since this is an instantaneous value, the curve of E.M.F. induced in a full-pitch coil is identical in shape and phase with that of the flux distribution. Further, as e changes its sign with B, it alternates with the frequency n cycles per second.

We have seen on page 33 that the instantaneous value of the sum of the E.M.F., generated in the conductors a, b, c to m of a uniformly distributed winding, is given by the expression

$$\Sigma_a^m e=2Tvl10^{-8}\left\{\mathsf{B}_1\frac{\sin\sigma}{\sigma}\sin\theta+\mathsf{B}_3\frac{\sin 3\sigma}{3\sigma}\sin 3\theta+\ldots\mathsf{B}_h\frac{\sin h\sigma}{h\sigma}\sin h\theta\right\}\text{.........}(4)$$

where σ is the angle subtended by half the coil breadth $=\frac{S}{\tau}\cdot\frac{\pi}{2}$. The factor $\frac{\sin h\sigma}{h\sigma}$ is the "winding factor" for the particular harmonic in question, where h is the order of the harmonic. Smith and Boulding have given the following table of winding factors for the odd harmonics up to the 25th for different spans of armature coil expressed as fractions of the pole-pitch.

UNIFORMLY DISTRIBUTED WINDINGS.

TABLE XV. VALUES OF WINDING FACTORS.

Spread of Winding S/τ =	1/6	1/3	1/2	2/3	1
Open winding. Closed ,,	— 12-PH.	3-PH. 6-PH.	2-PH. 4-PH.	(1-PH.) 3-PH.	— Diam. taps.
f_1	0·988	0·955	0·900	0·827	0·637
f_3	0·900	0·636	0·300	0·000	−0·212
f_5	0·738	0·191	−0·180	−0·165	0·127
f_7	0·527	−0·136	0·129	0·118	−0·091
f_9	0·300	−0·212	0·100	0·000	0·071
f_{11}	0·090	−0·087	0·082	−0·075	−0·058
f_{13}	−0·076	0·073	−0·069	0·064	0·049
f_{15}	−0·180	0·127	−0·060	0·000	−0·042
f_{17}	−0·217	0·056	0·053	−0·049	0·037
f_{19}	−0·194	−0·050	0·046	0·043	−0·033
f_{21}	−0·129	−0·091	−0·043	0·000	0·030
f_{23}	−0·043	−0·041	−0·039	−0·036	−0·028
f_{25}	0·040	0·038	0·036	0·033	0·025

By means of these winding factors, we can calculate the pressure $\Sigma_a^m e$ induced in a winding, distributed uniformly over any fraction of the pole-pitch, by any flux whose wave-shape is known. The above figures are of great interest, for they show the amount by which the flux harmonics are reduced in the pressure curve by the spread of the winding. For example, in a section of winding spread over the whole pole pitch ($S/\tau=1$), the magnitude of the winding factor in per cent. is $100/h$, *i.e.* in this case the winding factors bear the same ratio to one another numerically as the coefficients of the harmonics of a rectangle (see page 22).

The winding factor for any harmonic, h, can also be represented graphically as the ratio of the length of the chord to the length of the arc in subtending an angle $h\frac{S}{\tau}\pi$ radians at the centre of a circle (see page 112). The arc of the circle represents the actual E.M.F.'s induced in the several coils, whilst the chord represents the resultant of these E.M.F.'s, which are slightly out of phase with one another.

Another interesting feature arising from the spread of the winding is the fact that under certain circumstances harmonics which may be present in the flux curve, and therefore in the E.M.F. of each conductor also, disappear entirely from the phase pressure. The conditions for this can be directly deduced from the general expression for the winding factor. In order that any particular harmonic, h, shall not reappear in the pressure, $\Sigma_a^m e$, if present in the B-curve, it is sufficient and necessary that $f_h=0$, or $\sin h\frac{S}{\tau}\frac{\pi}{2}=0$ (since the denominator $h\frac{S}{\tau}\frac{\pi}{2}$ obviously can never be zero). Now,

$$\sin h\frac{S}{\tau}\frac{\pi}{2}=\sin X\pi=0 \text{ only holds when } X=\frac{h}{2}\frac{S}{\tau}=0,\ 1,\ 2,\ \text{etc.} \ldots .$$

h, of course, being any odd integer.

For example, let $S/\tau=2/3$, then $X=\frac{h}{2}\frac{2}{3}=\frac{h}{3}$. It is at once seen that X will be integral when, and only when, $h=3, 9\ldots$, etc. So that with $S/\tau=2/3$, no harmonic whose order is a multiple of 3 will appear in the pressure wave. This is an important result, for it shows that there can never be a third harmonic in the line pressure of a star-connected, three-phase generator, nor in the alternating pressure of a three-phase rotary converter, nor in the phase pressure when each phase extends over 2/3 of τ, nor in the pressure of a single-phase alternator with two-thirds of the periphery wound. It will be seen that this also holds when the winding is placed in slots instead of being uniformly distributed. The absence of the third, ninth, etc., harmonics is noticed in the above table in the column where $S/\tau=2/3$.

Again, in a similar way with $S/\tau=2/5$ or 4/5, it can be shown that no harmonic which is a multiple of 5 can appear in the pressure curve. The important case in practice, however, is the one previously referred to when $S/\tau=2/3$; and it is to be noticed that by making the phase-band two-thirds of the pole-pitch, it is possible to have a star-connected, three-phase winding without a third harmonic in the wave-form of the E.M.F. generated in one leg of the star.

In order to arrive at the wave-form of the E.M.F., it is necessary to know the form of the B-curve, or, in other words, to know the values of the coefficients B_1, B_3, B_5, etc. (see page 22). Where the B-curve is irregular in form the coefficients can be determined by any of the methods of harmonic analysis.

It may be that in a machine with a salient pole the rectangular form depicted in Fig. 322 is sufficiently near the truth for the purpose of arriving at the approximate value of the coefficients. We then have

$$B=\frac{4}{\pi}B_g(\cos\alpha\sin\theta_x+\tfrac{1}{3}\cos 3\alpha\sin 3\theta_x+\ldots). \quad\ldots\ldots(5)$$

The coefficients of the various terms here depend upon the ratio of pole-arc to pole-pitch.

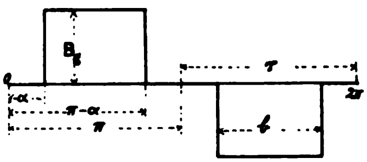


FIG. 322.—Rectangular field-form.

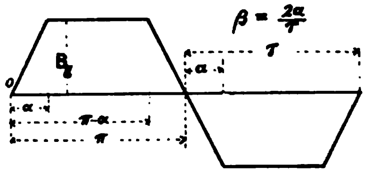


FIG. 323.—Trapezium field-form.

Taking the ratio of pole-arc, b, to pole-pitch, τ, as two-thirds, which is usual for slow-speed alternators, the equation for the flux distribution is found by substituting: $\alpha=\frac{\pi}{6}=30°$ in Eq. 5:

$$B_x=\frac{4}{\pi}B_g\frac{\sqrt{3}}{2}(\sin\theta_x-\tfrac{1}{5}\sin 5\theta_x-\tfrac{1}{7}\sin 7\theta_x+\tfrac{1}{11}\sin 11\theta_x+\ldots).$$

It is seen that with this ratio of pole-arc to pole-pitch, all harmonics whose orders are multiples of 3 vanish in the flux curve, so that there can be no third, ninth, etc., harmonics in the pressure waves. For the remaining flux harmonics:

$$\frac{B_5}{B_1}=-\frac{1}{5};\quad\frac{B_7}{B_1}=-\frac{1}{7};\quad\frac{B_{11}}{B_1}=+\frac{1}{11},\ \text{etc.},$$

also
$$B_g=\frac{\phi}{\frac{2}{3}\tau l}=\frac{3}{2}\frac{\phi}{\tau l}$$

and
$$B_1=\frac{4}{\pi}B_g\frac{\sqrt{3}}{2}=\frac{3\sqrt{3}}{\pi}\frac{\phi}{\tau l}.$$

Substituting these values in Eq. (4), p. 306, we get for the constant term :

$$2Tvl\,10^{-8}\mathsf{B}_1 = 2T2n\tau l\,10^{-8}\frac{3\sqrt{3}}{\pi}\,\frac{\phi}{\tau l} = \frac{12\sqrt{3}}{\pi}Tn\phi\,10^{-8},$$

so that the equation for the pressure $\Sigma_a^m e$ with a rectangular flux distribution over two-thirds of the pole-pitch becomes

$$\Sigma_a^m e = \frac{12\sqrt{3}}{\pi}Tn\phi 10^{-8}(f_1 \sin\theta - \tfrac{1}{5}f_5 \sin 5\theta - \tfrac{1}{7}f_7 \sin 7\theta + \text{etc.}). \qquad (6)$$

From this general equation, the E.M.F. curve for any given spread of the armature winding can be found. Several interesting cases are worked out in the paper referred to above.

Similarly from the general equation of the trapezium (see Fig. 323),

$$\mathsf{B}_x = \frac{8}{\pi^2}\,\frac{\mathsf{B}_g}{\beta}\left(\sin\beta\frac{\pi}{2}\sin\theta_x + \frac{1}{9}\sin 3\beta\frac{\pi}{2}\sin 3\theta_x + \ldots\right),$$

the coefficients of the various terms in equation (4), page 306, are worked out. These are especially interesting as they refer to the alternator with a cylindrical field-magnet (see page 377).

Effect of the slots. The spacing ripple. If Z denotes the total number of slots in the periphery, and Q the number per pole, then the slot-pitch in radians will be : $= \frac{2p.\pi}{Z} = \frac{\pi}{Q} = \gamma$. The slot-pitch γ then denotes the angle between successive coil-sides. We now have to find the sum of the E.M.F.'s induced in the m coils displaced from one another by the angle γ (see Fig. 324). This depends upon the value of the winding factors of the harmonics in the field-form.

Where the pole-pitch is exactly divisible by the slot-pitch, *i.e.* Q is integral, the actual coils can be replaced by full-pitch coils (see Fig. 116). Then, from Eq. (3a), p. 306, for m full-pitch coils in series:

$$\Sigma_a^m e = 2T_c vl\,10^{-8}\Sigma_a^m\mathsf{B}_x,$$

$$\begin{aligned}\Sigma_a^m\mathsf{B}_x &= \mathsf{B}_a + \mathsf{B}_b + \ldots + \mathsf{B}_m \quad \text{(see Fig. 324)}\\ &= \mathsf{B}_1(\sin\theta_a + \sin\theta_b + \ldots + \sin\theta_m) \quad \text{(see Eq. (1), p. 22)}\\ &\quad + \mathsf{B}_3(\sin 3\theta_a + \sin 3\theta_b + \ldots + \sin 3\theta_m),\\ &\quad + \text{etc.}\\ &= \mathsf{B}_1\frac{\sin\left(\theta a + \frac{m-1}{2}\gamma\right)\sin\frac{m\gamma}{2}}{\sin\frac{\gamma}{2}} + \mathsf{B}_3\frac{\sin 3\left(\theta a + \frac{m-1}{2}\gamma\right)\sin 3\frac{m\gamma}{2}}{\sin 3\frac{\gamma}{2}} + \text{etc.}\end{aligned}$$

Now, $\theta_a + \frac{m-1}{2}\gamma = \frac{\theta_a + \theta_m}{2} = \theta =$ displacement of midpoint of the m coil-sides ; hence,

$$\Sigma_a^m e = 2T_c vl\,10^{-8}\left\{\mathsf{B}_1\frac{\sin m\frac{\gamma}{2}}{\sin\frac{\gamma}{2}}\sin\theta + \mathsf{B}_3\frac{\sin 3m\frac{\gamma}{2}}{\sin 3\frac{\gamma}{2}}\sin 3\theta + \text{etc.}\right\}.$$

Since $\dfrac{\sin h\frac{m\gamma}{2}}{\sin h\frac{\gamma}{2}} < m$, the harmonics in the pressure $\Sigma_a^m e$ of m coils in series will be less than m times the harmonics in the coil pressure.

Inserting $T = m \cdot T_c$ = total turns in series, the general expression for the pressure induced in m coils at angle γ apart becomes:

$$\Sigma_a^m e = 2TvL10^{-8}\left\{B_1\frac{\sin\frac{m\gamma}{2}}{m\sin\frac{\gamma}{2}}\sin\theta + B_3\frac{\sin 3\frac{m\gamma}{2}}{m\sin 3\frac{\gamma}{2}}\sin 3\theta + \ldots\right\}$$

$$= 2TvL10^{-8}(\mathsf{B}_1 f_1 \sin\theta + \mathsf{B}_3 f_3 \sin 3\theta + \ldots) = 2TvL10^{-8}\mathsf{B}_1\left(f_1\sin\theta + \frac{\mathsf{B}_3}{\mathsf{B}_1}f_3\sin 3\theta + \ldots\right).$$

This is of the same form as Eq. 4 for distributed windings, but the winding factors are now:

$$f_1 = \frac{\sin\frac{m\gamma}{2}}{m\sin\frac{\gamma}{2}}, \quad f_3 = \frac{\sin 3\frac{m\gamma}{2}}{m\sin 3\frac{\gamma}{2}},$$

....

$$f_h = \frac{\sin h\frac{m\gamma}{2}}{m\sin h\frac{\gamma}{2}} = \frac{\sin h\frac{m\pi}{Q2}}{m\sin h\frac{1}{Q}\frac{\pi}{2}}, \quad \text{since } \gamma = \frac{\pi}{Q}.$$

These are seen to be different from the winding factors for uniformly distributed windings given on page 306, and we must now investigate the influence of this on the shape of the pressure curve. In the present case Q is an integer, *i.e.* any odd

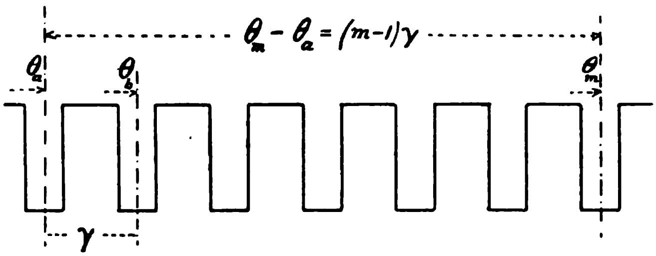


FIG. 324.

or even number, so that $2Q$—the number of slots in a double pole-pitch corresponding with a complete period—will always be even. Further, in steady flux curves, with the positive and negative parts identical, only odd harmonics are present; hence $2Q \pm 1$, $2Q \pm 3, \ldots$, $2Q \pm x$ and $M \cdot 2Q \pm x$ will be possible values of h for the harmonics B_h, where x is any odd number and M any whole number.

For these particular harmonics, winding factors become:

$$f_{(2Q-1)} = f_{(2Q+1)} = \frac{\sin\frac{2Q \pm 1}{Q}m\frac{\pi}{2}}{m\sin\frac{2Q \pm 1}{Q}\frac{\pi}{2}} = \pm\frac{\sin\frac{m}{Q}\frac{\pi}{2}}{m\sin\frac{1}{Q}\frac{\pi}{2}} = \pm f_1.$$

Similarly, $\quad f_{(2Q-3)} = f_{(2Q+3)} = \pm f_3,$

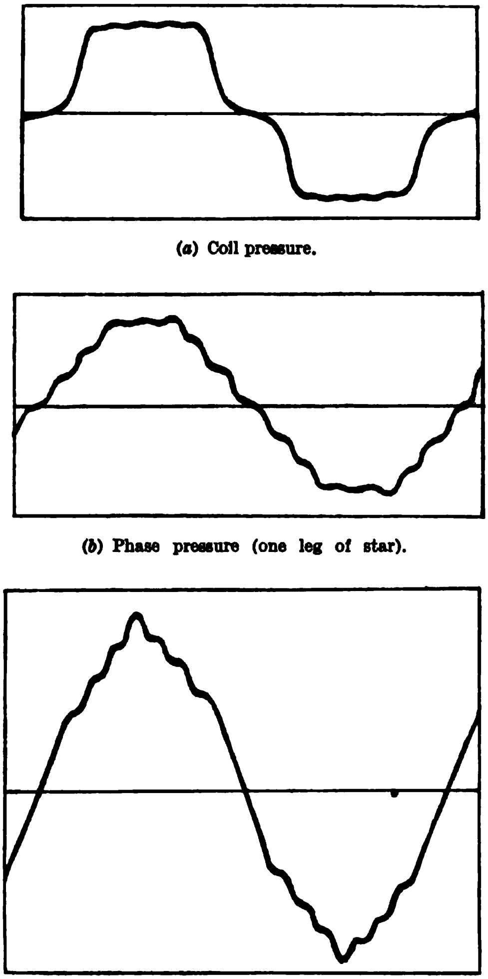
and $\quad f_{(2Q-x)} = f_{(2Q+x)} = \pm f_x,$

and $\quad f_{(M2Q-x)} = f_{(M2Q+x)} = \pm f_x.$

Thus when there is a whole number of slots per pole, the winding factor does not decrease as the order of the harmonic h increases in the same way as with uniformly distributed winding, but periodically rises to a maximum (numerically $= f_1$) whenever h passes a multiple of $2Q$. For example, with $Q = 6$ or $2Q = 12$, as in a three-phase winding with two slots per pole and phase ($m = q = 2$), we get $f_h = f_{(M.2Q-1)} = f_{(M.2Q+1)} = \pm f_1$ when $h = 11, 13; 23, 25; 35, 37; 47, 49;$ etc. (cp. Table XVI.).

This means that if any of these harmonics are present in the B-curve, they will reappear in the pressure curve $\Sigma_a^m e$ with the same percentage value as the fundamental, whilst the other harmonics are largely reduced by their winding factors. Thus any of these harmonics will give rise to a ripple on the fundamental.

Since this effect is due to the spacing of the armature coils, Dr. S. P. Smith has given to it the term "*spacing ripple.*" It is easy to see that this ripple in



(*a*) Coil pressure.

(*b*) Phase pressure (one leg of star).

(*c*) Terminal pressure (two legs in series).

FIG. 325.

the pressure wave will be mainly due to harmonics of the orders $2Q \pm 1$, since the values of B_h become very small for $M \cdot 2Q \pm 1$ when $M > 1$. Also the harmonics of the orders $2Q \pm 3$, with winding factors numerically equal to f_3, will not be nearly

so important as the $(2Q+1)$th. Again, as the number of slots per pole increases, $B_{(2Q+1)}$ usually decreases and the spacing ripple becomes less pronounced. For example, in a three-phase winding with 6 slots per pole and phase, $2Q \pm 1 = 35$ and 37, and both B_{35} and B_{37} are very small even with a rectangular flux distribution.

The oscillograms reproduced in Fig. 325 (*a*), (*b*) and (*c*) taken off a machine with semi-enclosed slots, having three slots per pole per phase, clearly show the spacing

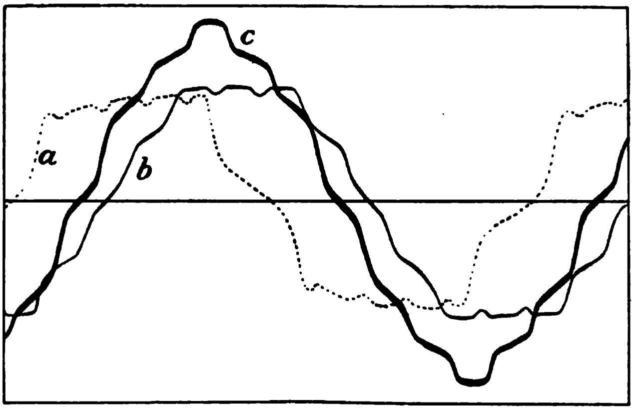


FIG. 326.

ripple. In this machine, the flux curve is fairly rectangular and smooth, as seen in the curve marked "coil pressure," showing that there is practically no swinging of the flux, so that the ripple is almost entirely due to the spacing of the coils. The magnitude of the spacing ripple can easily be calculated when the harmonics

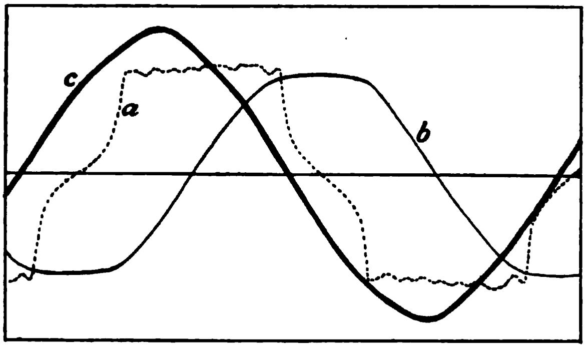


FIG. 327.

in the B-curve are known. Fig. 326 gives the three curves superimposed for a machine having only two slots per pole per phase. Here the main indentations on the terminal pressure are due to the "spacing ripple." Some slight "tooth ripples" (see page 313) are visible in the flux and phase-pressure.

The winding factors for each phase of ordinary 3-phase open windings with a whole number of slots per pole are given in Table XVI. To find the corresponding

winding factors for the terminal E.M.F., or the E.M.F. of a single-phase winding with two-thirds of the slots wound, the values in the table must be multiplied by $\cos h\,30°$.

TABLE XVI. WINDING FACTORS FOR PHASE E.M.F. OF 3-PHASE WINDINGS IN SLOTS.

$q=$	2	3	4	5	6	7	8	9	10	$s/\tau=\frac{1}{3}$.
f_1	·966	[illegible]	·958	·957	·957	·957	·956	·955	·955	·955
f_3	·707	·667	·654	[illegible]	·644	[illegible]	·641	[illegible]	·639	·636
f_5	·259	·217	·205	·200	·197	·195	·194	·194	·193	·191
f_7	− ·259	− ·177	− ·158	− ·149	− ·145	− ·143	− ·141	− ·140	− ·140	− ·136
f_9	− ·707	− ·333	− ·270	− ·247	− ·236	− ·229	− ·225	− ·222	− ·220	− ·212
f_{11}	− **·966**	− ·177	− ·126	− ·110	− ·102	− ·097	− ·095	− ·093	− ·092	− ·087
f_{13}	− **·966**	·217	·126	·102	[illegible]	·086	·083	·081	·079	·073
f_{15}	− ·707	[illegible]	·270	[illegible]	·172	·158	·150	[illegible]	·141	·127
f_{17}	− ·259	[illegible]	·158	·102	·084	·075	·070	·066	·064	·056
f_{19}	·259	**·960**	− ·205	− ·110	− ·084	− ·072	− ·066	− ·062	− ·060	− ·050
f_{21}	·707	·667	− ·654	− ·247	− ·172	− ·143	− ·127	− ·118	− ·112	− ·091
f_{23}	**·966**	·217	− **·958**	− ·149	− [illegible]	− ·072	− ·063	− ·057	− ·054	− ·041
f_{25}	**·966**	− ·177	− **·958**	·200	·102	·075	·063	·056	[illegible]	·038
f_{27}	·707	− [illegible]	− ·654	·646	·236	·158	·127	·111	·101	·071
f_{29}	·259	− ·177	− ·205	**·957**	·145	[illegible]	·066	·056	·050	·033

The chief harmonics in the spacing ripple, *i.e.* $2Q\pm 1$, are in heavy type.

Where the number of slots per pole is fractional, the effect of the spacing ripple is very much reduced, as explained on page 305. Fig. 327 is an oscillogram taken from a machine having 6·75 slots per pole. Here the spacing ripple so apparent in Fig. 326, where $Q=6$, has entirely disappeared, and we get for both the phase pressure and the terminal pressure quite smooth curves as with uniformly distributed windings.

(*a*) FIG. 328. (*b*)

Pulsations due to the teeth. The tooth ripple. When a number of teeth like those depicted in Fig. 328 are rotating under a pole they produce pulsations of the flux of two kinds : (1) a pulsation in the total flux per pole, due to a change in the reluctance of the air-gap as the armature changes from position (*a*) to position (*b*) ; and (2) a swinging to and fro of the flux along the periphery as a tooth under the horn of the pole is replaced by a slot. Both of these effects can be very much

diminished by suitably bevelling the pole so that the reluctance under the polar horn is almost constant for any position of the armature. The skewing of the slots or of the polar horn by a full slot pitch (see Fig. 533) is also a cure.

We do not in practice find very much pulsation in the amount of the total flux per pole, because such a pulsation would be opposed by eddy-currents in the solid parts of the magnetic circuit, and by alternating currents induced in the exciting circuit. The swinging of the flux, however, may give rise to very

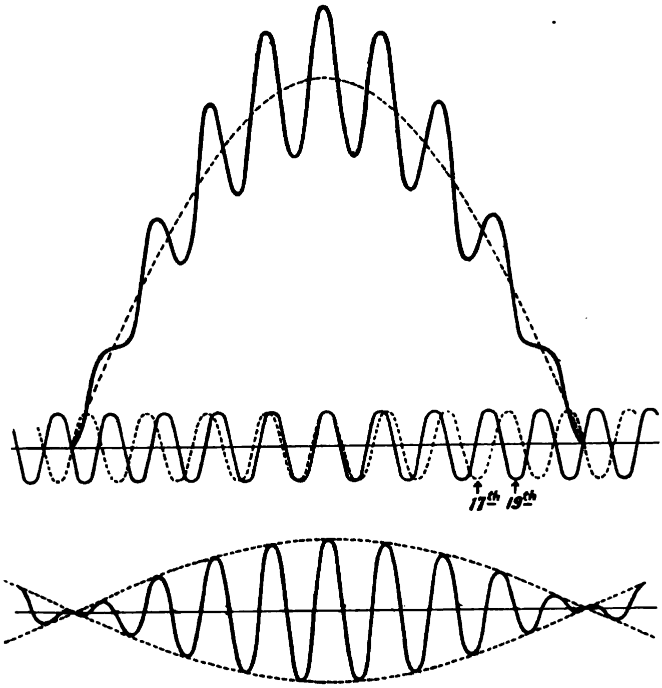
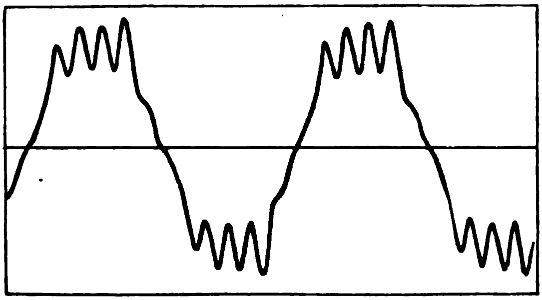


FIG. 329.—Calculated tooth ripple due to swing of sinusoidal flux. $Q=9$ slots per pole.

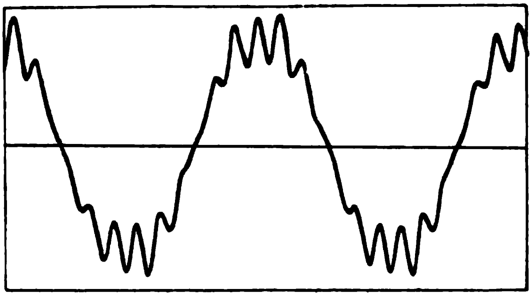
noticeable ripples in the wave-form. Smith and Boulding employ the name of "tooth ripples" for these, to distinguish them from the "spacing ripples" described on page 310.

When a tooth approaches the horn at the right side of a pole, it reduces the reluctance of the air-gap between itself and the pole, and the fringing flux extending to it rapidly increases. At the left side of the pole a slot may be taking the place of a tooth, so that the fringing flux on that side is rapidly diminishing, and the disposition of the flux becomes unsymmetrical with regard to the centre line of the pole. After the movement of the armature through one-half a tooth-pitch, the flux is again unsymmetrical; but now the heavy fringing is on the left side and the lighter fringing on the right. Such a swinging to and fro of the flux gives rise to E.M.F.'s in all the conductors under the pole, which are superimposed upon the E.M.F.'s generated by the uniform movement of the conductors. The sum of the effects in all the conductors in a phase-band is greatest when B is greatest, that is

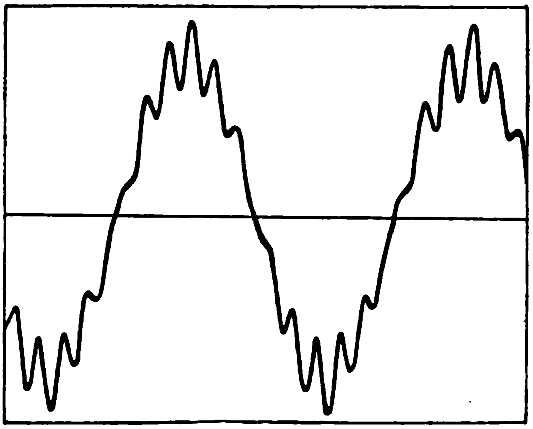
when the phase-band is opposite a pole, and is least when the phase-band is between the two poles. Thus the ripples due to swinging of the flux are greatest



(a) Coil pressure.



(b) Phase pressure.



(c) Terminal pressure.

FIG. 330.—Oscillograms taken on 3-phase machine having 6 open slots per pole, and showing the "tooth ripple" in a marked degree.

on the crest of the wave, and sink to a minimum as the main wave passes through zero, as will be understood by reference to Fig. 329. The exact shape of the ripples is, of course, very complex; but as a first approximation we may take them as

sinusoidal. On this assumption, it can be shown that where the flux from the pole is sinusoidal, the effect of the tooth ripple can be expressed by the addition of a term to the ordinary expression for the E.M.F. in a coil. Thus the E.M.F. in a coil of T_c turns becomes:

$$e = 2\pi n T_c \phi 10^{-8} (\sin\theta_1 - \eta\, 2Q \sin\theta_1 \cos 2Q\theta_1)$$

where η is the amplitude of the tooth ripple, and Q is the number of slots per pole.

Now, $$-2\sin\theta_1 \cos 2Q\theta_1 = \sin(2Q-1)\theta_1 - \sin(2Q+1)\theta_1,$$

so that the tooth ripple can be analysed into two odd harmonics, the $(2Q-1)$th and the $(2Q+1)$th, each having an amplitude equal to half the maximum amplitude of the ripple as seen on the crest of the wave. In Fig. 329 are shown the tooth ripples occurring in a machine having 9 slots per pole: $2Q = 18$. Along the base-line are plotted the seventeenth and nineteenth harmonics, which when combined give the characteristic shape of the tooth ripple.*

Fig. 330 gives the coil pressure, the phase pressure and the terminal pressure of a 3-phase machine having 6 open slots per pole. The fact that the flux was swinging is shown by the ripples on curve (*a*). These tooth ripples appear undiminished in phase pressure (*b*), and the terminal pressure (*c*). The dissymmetry of the curves is probably due to the hysteresis of the iron of the armature.

Where the phase-band is made up of m coils connected in series, the amplitude of the ripple in the phase pressure $\Sigma_a^m e$ depends upon the value of the winding factor of the $(2Q-1)$th and $(2Q+1)$th harmonics. Now, it was shown on page 310 that when the field system is normal and Q is integral:

$$f_{(2Q-1)} = f_{(2Q+1)} = \pm f_1,$$

so that in this case the tooth ripple occurs in the phase pressure with the same percentage value as in the coil pressures.

Where the number of slots per pole is fractional, the effect is reduced just in the same way as with the spacing ripple. It is as if the number of slots per pole were increased (see page 305).

CALCULATION OF A 750 K.V.A. ENGINE-DRIVEN ALTERNATING-CURRENT GENERATOR, TO RUN AT A SPEED OF 375 REVS. PER MINUTE.

2100 VOLTS; 3 PHASES; 50 CYCLES.

In going through the calculation given below, it may be convenient to refer to Fig. 331, which shows the generator in question drawn to scale. Fig. 332 gives a sectional elevation, and Fig. 333 gives details of the poles and field-coils.

We will suppose that we have obtained an order for a 750 K.V.A. generator to comply with Specification No. 1. The size of the frame upon which such a machine would be built would depend upon the particular sizes of frames which the manufacturer might already have; but if we were to start *de novo*, the considerations which would settle the diameter and length are those given on page 299.

* For discussion on question whether the tooth ripple is symmetrical, see remarks by C. C Hawkins and Dr. G. W. O. Howe, *Journ. I.E.E.*, vol. 53, pp. 241, 243; also *Electrician*, vol. lxxiii., pp. 3, 367. 417, 456, 497, 537.

The higher the peripheral speed, the better the specific use we make of our copper and iron; but if we choose too high a peripheral speed by making the diameter great, we find that the axial length of the generator becomes short as compared with the pole pitch, and the cost of construction comes out higher than one would at first suppose from the mere statement of the weight of active material. Moreover, if we make the peripheral speed much higher than 6000 feet per minute, it will be found necessary to adopt a special construction of field spider in order to provide against the great centrifugal forces. A peripheral speed of 6000 feet per minute for a 50-cycle generator gives a ratio of pole pitch to axial length which is very economical, and no expensive construction of field-frame need be resorted to.

We will therefore decide on a peripheral speed of 6000 feet per minute, or say 30 metres per second; and we will take the internal bore of the stator as 155 cms. In the calculation sheet given on page 321 the dimensions are given both in inches and centimetres.

This calculation sheet is designed so that the same general form can be used either for an A.C. generator, a C.C. generator, an asynchronous motor, a synchronous motor, or a rotary converter. As explained above on page 8, the same general method will be used when calculating all these machines, and it is an advantage to be able to compare the figures of one type of machine with those of another type on the same form. The first three lines of the form in question deal with the performance of the machine which it is intended to design; after the date comes a statement of the type of machine, whether a turbo- or engine-type, belted or geared, open or enclosed, etc. The number of poles is of course an important matter, which naturally takes a conspicuous place. Then comes the electrical specification number, in one corner for easy reference. The second line is self-explanatory. In the third line it is necessary to insert the amperes per conductor, because sometimes there are several paths in parallel through a machine. For a C.C. machine or rotary converter the amperes per brush arm should be stated. The temperature rise and regulation and over-load capacity are all matters relating to the guaranteed performance. The next line deals mainly with records, such as the name of the customer, the order number, the quotation number where a quotation has been made previous to the order, and the performance specification number. The fourth and fifth lines deal with important data belonging to the size of frame employed. The frame number, which is sometimes specified by giving the diameter of the bore and the length of iron. The amount of air required for cooling, if the machine is of the turbo-type. The circumference of the active surface; the gap area or area of active surface; the $A_g\mathrm{B}$, for which see page 6. Above this it is convenient to write the greatest possible $A_g\mathrm{B}$ that can be put on the frame in question. The I_aZ_a, for which see page 8, and the greatest possible I_aZ_a. The $\frac{I_aZ_a}{\text{circumference}}$ gives the ampere wires per centimetre of periphery, a very important quantity in judging the rating of the frame. Then comes the output coefficient. The next line gives the K_e, for which see page 23; the voltage formula; the ampere-turns per pole on the armature; and lastly, the total maximum ampere-turns on all the poles. The left-hand side of the form then deals with the armature, which may be either revolving or stationary; and the right-hand side deals

FIG. 331.

A 750 K.V.A. 3-phase generator, 2100 volts, 50 cycles,

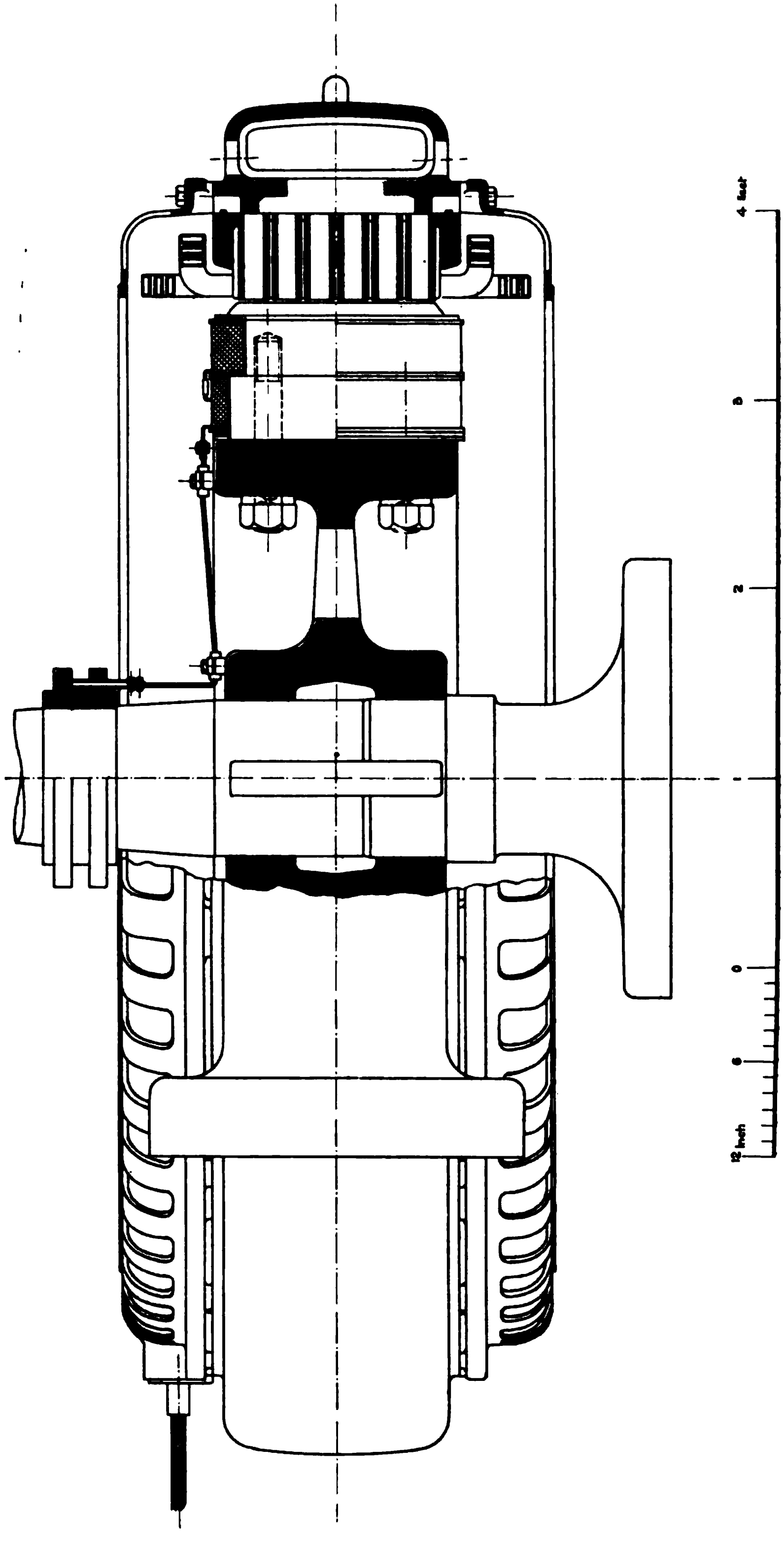


Fig. 332

375 R.P.M., designed to meet Specification No. 1, page 269.

with the field-magnet, which may be either stationary or revolving, the word that does not apply being struck out in each case. The general method of using the form will be best understood from the example given below. Two columns are provided for the insertion of figures on each line. The purpose of these two columns is not, as might be supposed from an inspection of the form on page 321, for the insertion of both centimetre and inch units; for each engineer will as a rule confine himself to the system of units which he prefers. The second column is to enable the figures for an alternative design to be put alongside those of the principal design, in order that comparison may be conveniently made. We have used the second column on page 321 for the insertion of the dimensions expressed in inches, for the convenience of such readers as are more familiar with those units. The rough diagrams of the slots, teeth and poles are on the original form drawn so that by means of a few simple lines it is easy to represent either open slots or semi-closed slots, and various shapes of pole.

We will proceed, then, to fill in our calculation sheet for the 750 K.V.A. engine-type generator to give a three-phase current (power factor 0·8) at any voltage from 2000 to 2100. The amperes per terminal will be 206; the cycles per second, 50; the R.P.M., 375. The amperes per conductor in this case would be 206. According to the specification on page 270, the temperature rise by resistance after full-load run will be 55° C.; the regulation on unity power factor 8 per cent.; and the overload, 25 per cent. for two hours. If we adopt an inside diameter of stator punchings of 155 cms., we arrive at a circumference of 486 cms. The final fixing of the exact length of iron cannot be done until the design has proceeded somewhat further, but a preliminary length can be worked out from what we know to be a suitable D^2l constant. For a high-speed engine-driven generator having the performance specification on page 270, a suitable D^2l constant would be 4×10^5 cm³.; this would give us about 32 cms. length of iron; and if one of our standard frames happened to be 12½ ins. long, or 31·8 cms., we would make an attempt to get the machine on that frame. Multiplying the circumference by 31·8, we get A_g, the gap-area or area of active face equal to 15,400 sq. cms. If we could have a flux density in the gap of 10,000, this would give us a possible $A_g\mathrm{B}$ of about $1{\cdot}5 \times 10^8$. Applying the voltage formula ((1), page 24), we would arrive at a total number of conductors of about 592. But it is convenient to have the number of conductors a multiple of 48 or 16 times 3, so a more suitable number is 576. We can then have 144 slots with 4 conductors per slot. 144 slots gives us 9 slots per pole, or 3 slots per phase per pole. We ought to say something here about the considerations which settle the number of slots per phase per pole. The cheapest arrangement of conductors is of course one which employs a small number of slots; because it is necessary to insulate each coil for full voltage to earth, and as we increase the number of coils, we increase the space taken up by insulation as well as the cost of the insulation. If we were to employ only 1 slot per phase per pole in the machine under consideration, we should have about 2500 amperes per slot. The amount of heat generated per coil would be three times as great as in the arrangement proposed, and as the cooling surface of the coil would not be increased in proportion, the heating would be excessive; unless, indeed, a greater cross-section of copper were used. Moreover, the wave-form of the generator would

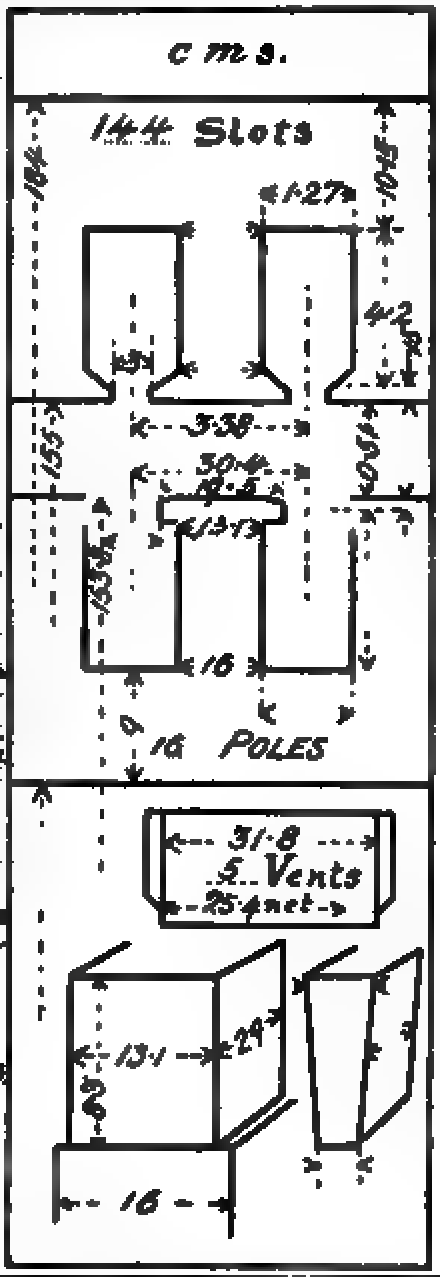
Date 20 June 1912. Type E.T. A.C. GEN. ~~or Motor Rotary~~ 10 Poles. Elec. Spec. 1
K V A 750 ; P F ·8 ; Phase 3 ; Volts 2000–2100 ; Amps per ter 206 ; Cycles 50 ; R.P.M. 575 ; Rotor Amps
~~H.P.~~ Amps p. cond. 206 ~~Amps p. br. arm~~ Y. Temp. rise 55° C. Regulation 8% Overload 25% 2 hrs.

Customer ASTOR COAL CO. ; Order No. 6421 . Quot. No. 6765R ; Perf. Spec. A.C. 642 ; Fly-wheel effect –

Frame 184/32 Circum. cm. 486 ; Gap Area sq. cm. 12400 ; poss. A_g B $1·6 \times 10^8$; poss. $I_a Z_a$ 120 000 ; $I_a Z_a$ 244 ; $D^2 L \times R P M$ 542×10^6 cm.³
Air ; A_g B $1·40 \times 10^8$; $I_a Z_a$ 119 000 ; Circum. ; K.V.A. $2·92 \times 10^6$ in.³

K_a 0·4 ; 2100 Volts = ·4 × 0·26 × 576 × 1·40 × 10^8 ; Arm. A.T. p. pole 3800 Max. Fld. A.T. 125 000

Armature. ~~Rev.~~ Stat.		cm.	inch.
Core	Dia. Outs	184	72½
	Dia. Ins.	156	61
	Gross Length	31·8	12½
	Air Vents	5 – ·635	5 – ·25
	Opening Min. Mean		
	Air Velocity	30 m.	
	Net Length 28·6 × ·89	25·4	10
	Depth b. Slots	10·16	4
	Section 258 Vol	142000	
	Flux Density	11600	12·5 KL
	Lo[illegible] al	8500	
	Bt[illegible] al	13850	16850
	Ga[illegible] ts	5900	
	Vent Area 28000 Wts	6300	
	Outs. Area 33000 Wts	4950	17150
Teeth	No of Segs 12 Mn Circ.	500	
	No of Slots 144 × 1·27 =	183	
	K_a = 2	317	
	Section Teeth	8000	1240
	Volume Teeth	35000	
	Flux Density	18300	19·6 KL
	Loss. ·15 p. cu cm. Total	5250	
	Weight of Iron	1410 kg.	3100 lbs.
Conductors	Star ~~or Mesh~~ Throw	3–10, 2–11, 1–12	
	Cond. p. Slot	4	
	Total Conds	576	
	Size of Cond. cm. ·75 × ·72	·53 sq. c.	·082 sq. in.
	Amp. p. sq. cm.	385	2500 A.
	Length in Slots 32		
	Length outside 62 Sum	94	
	Total Length	540 m.	1770 ft.
	Wt. of 1,000 m. 466 kg Total	250 kg.	552 lbs.
	Res. p. 1,000 ·328 Total	·177	
	Watts p. metre	67	
	Surface p. metre	1050	
	Watts p. Sq. cm.	·064	
	·0012 × 15° C / ·25	·072	



Field ~~Stat. or~~ Rotor.	cm.	inch
Dia. Bore	153·8	60·6
½ Total Air Gap	0·51	0·2
Gap Co-eff. K_g	1·1	
Pole Pitch 30·4 Pole Arc	19·6	
K_f	·656	
Flux per Pole $5·96 \times 10^6$	$6·9 \times 10^6$	1130 KL.
Leakage n.l. ·841.1/·58	$7·54 \times 10^6$	1200
Area 379 Flux density	95000	
Unbalanced Pull	4200	9300 lbs.
No. of Seg. Mn. Circ.		
No. of Slots × =		
Vents		
K. Section		
Weight of Iron Poles 480 kg. Yoke 900 kg.		

	~~Shunt.~~ cm.	~~Series~~ inch	~~Compd.~~
A.T. p Pole n. Load	5760		
A.T. p. Pole f. Load	10500		
Surface	73500	11400	
Surface p. Watt	8·7	1·35	
I² R.	8500		
I. R.	104		
Amps.	81·5		
No. of Turns	75 + 54	= 129	
Mean l. Turn	1·07 & 1·12		
Total Length	1280' 970		
Resistance	0·8 + ·51	= 1·06 cold	1·28 hot
Res. per 1,000 m.	·43 & ·526		
Sizes of Cond.	·64 and	·58 c.m.	sq. wire
Conds. per Slot			
Total			
Length			
Wt. per 1,000 m.	358 & 292		
Total Wt.	744 kg	1650 lbs.	
Watts per Sq.			
Star or Mesh			
Paths in parallel			

Magnetization Curve.	Section.	Length.	1800 Volts. B.	A.T.p.	A.T.	2100 Volts. B.	A.T.p.	A.T.	2200 Volts. B.	A.T.p.	A.T.
Core	258	21	9950	5·7	120	11000	7	145	12070	7·6	160
Stator Teeth	8000	4·5	15700	24	103	18300	97	415	19200	160	690
Rotor Teeth											
Gap	15400	0·5	8150		3640	9500		4250	9950		4450
Pole Body	379	8·3	15500	25	210	18000	90	750	18900	150	1250
Yoke	360	20	8300	8·5	170	9700	10	200	10200	10·5	210
					4243			5760			6760

Commutator.	
Dia. Speed	
Bars	
Volts p. Bar	
Brs. p. Arm	
Size of Brs.	
Amps p. sq.	
Brush Loss	
Watts p. Sq.	

CALC. EFFICIENCY	1¼ load.	Full.	¾	½	¼
Friction and W	7·2	7·2	7·2	7·2	7·2
Iron Loss	13·8	13·8	13·8	13·8	13·8
Field Loss	10·2	9·0	7·9	6·8	5·6
Arm. &c. I²R	14·1	8·8	5·0	2·2	0·5
Brush Loss					
	45·3	38·8	33·9	30·0	27·1
Output	750	600	450	300	150
Input	795·3	638·8	483·9	330	177·1
Efficiency %	94·25	94	93	90·9	84·6

Mag. Cur Loss Cur
Perm. Stat. Slot
,, Rot Slot × =
Zig-zag
2 × × =
1·77 × =
End × × =
Amps. Tot
r = . X_s =
S_1/S_2 . r_2 = +

Imp √ + =
Sh. cir Cur
Starting Torque
Max. Torque
Max. H P
Slip
Power Factor

be of very irregular shape. Two slots per phase per pole would be a possible arrangement; but this would give as much as 1650 amperes per slot, a rather high figure for a small machine of this type. Three slots per phase per pole give us better cooling conditions, and at the same time a very smooth wave-form. If we were to try to put in 4 slots per phase per pole, we should find that the amount of room taken up by the insulation, in comparison with the room taken up by copper, would be excessive.

We shall therefore decide to have 576 conductors, there being 144 slots with 4 conductors per slot. Filling 576 in the voltage formula, we obtain:

$$2100 = 0{\cdot}4 \times 6{\cdot}25 \times 576 \times A_g\mathrm{B},$$

$$A_g\mathrm{B} = 1{\cdot}46 \text{ volt-lines.}$$

On 50-cycle generators of this type it is well to have a ventilating duct for every 5 cms. of iron (see page 254). This will give us, say, 5 ventilating ducts, each 0·635 cm. wide. The net length is then obtained by multiplying 31·8 − 3·2 by 0·89: thus we get 25·4. To see whether this is sufficient, we must fix upon the size of slot, and this depends upon the size of conductor to be employed. The final fixing of the size of conductor will depend upon the cooling conditions of the armature coil; but as a preliminary figure we may, for an armature of this kind, assume 380 amperes per sq. cm. This suggests a conductor of a size 0·75 × 0·75 cm., having an area of 0·53 sq. cm., allowing for the rounded corners. A suitable thickness of insulation between copper and iron, consisting of mica and manilla paper, for a 2100-volt generator, is 0·2 cm. (see page 202). Adding the double thickness, 0·4 cm., to 0·75 cm. of copper, we arrive at 1·15 for the net thickness of copper and insulation. We should add to this an additional allowance of 0·12 cm. for the staggering in the building-up of the punchings and for air spaces. This gives a total width of slot of 1·27. In calculating the depth of slot required, we must not forget that it is advisable to place built-up mica between each conductor: so that 4 conductors and their insulations would take up 3·4 + 0·44 cms.; and allowing an additional 0·35 cm. for a fibre wedge, we arrive at 4·2 cms. for the length of slot.

We can now proceed to find the maximum flux-density in the iron teeth. This we do by dividing the total section of all the teeth into the total $A_g\mathrm{B}$.* As the sides of the slots are parallel, the sides of the teeth will not be perfectly parallel: so that the density of the flux will not be uniform all along the teeth. In cases where the change in the flux-density is very great, it is desirable to adopt a special method for considering it (see page 73); but in cases of this kind, where the diameter of the armature is great as compared with the depth of the slots, it is sufficient to take account of the flux-density at a point

* According to the older method of calculating A.C. generators, in which the flux per pole is taken as the quantity from which all flux-densities are calculated, it would be usual to estimate the number of teeth per pole and divide the area of the cross-section of these teeth into the total flux of the pole. The number of teeth per pole is a quantity which we cannot be very certain of where the pole is bevelled or where the flux-density tails off towards a neutral line. It will be remembered that the quantity $A_g\mathrm{B}$ is arrived at by multiplying the maximum B by the whole area of the gap. If, therefore, we divide the whole area of the teeth into the quantity $A_g\mathrm{B}$, we arrive at the maximum density of the teeth at no load, or at any other load for which we are given the maximum flux-density in the gap.

one-third of a tooth length from the narrowest part of the tooth. This can be obtained with sufficient accuracy by the following method: for teeth external to the air-gap add to the diameter 0·66 of the length of the teeth, multiply the sum by π, and thus obtain the circumference of the mean circle drawn around the machine, passing through points one-third of a tooth length from the narrowest part. The circumference in this case is 500 cms. Subtract from this the total width of all the slots, $144 \times 1{\cdot}27 = 183$. This gives us a total width of the teeth of 317. Multiplying by the net length, 25·4, we arrive at 8000 sq. cms. for the section of all the teeth. Dividing this into $1{\cdot}46 \times 10^8$, we arrive at 18,300 C.G.S. lines per sq. cm. As this is not an excessive flux-density for the teeth of a 50-cycle generator, we may fix on the gross length of 31·8 cms. as suitable. Referring to the iron loss curve (Fig. 29), we find that the loss per cu. cm. of iron is 0·15 watt. Now the volume of all the teeth is $8000 \times 4{\cdot}35 = 35{,}000$ cu. cms., giving a total loss in all the teeth of 5250 watts. We will now consider the depth of core below the slots: this will in general depend somewhat upon the standard size of frame and the depth of the slots. It is sufficient in a 50-cycle machine to provide such a depth that the flux-density does not exceed 12,000 C.G.S. lines per sq. cm. In this case we have taken the outside diameter of the punchings at 184 cms.; this gives a depth below the slots of 10·15 cms., a cross-section of 258 sq. cms., and a volume of 142,000 cu. cms. As the loss per cu. cm. is 0·06 watt, the total loss behind the slots is 8500 watts. We will now return to the armature conductors. We may take the length for the slots at approximately 32, and a length of end-connectors of 62, giving a sum of 94 cms.; so that 576 conductors would give a total length in all phases of 540 metres. The calculation of the total weight of armature copper is most easily carried out without reference to any wire table by remembering that 1000 metres of copper wire having a section of 1 sq. cm. weigh 875 kgs. If, therefore, we multiply 875 by the section of the conductor, in this case 0·53 sq. cm., we obtain the weight 464 kgs. per 1000 metres; so that 540 metres weigh 250 kgs. To obtain the resistance of any wire per 1000 metres at 20° C., we have the rule: divide 0·174 by the cross-section in sq. cms. $0{\cdot}174 \div 0{\cdot}53 = 0{\cdot}328$ ohm per 1000 metres; so that the total resistance of all phases is 0·177 ohm. The total I^2R loss in the armature at full load will be $0{\cdot}177 \times 1{\cdot}2 \times 206 \times 206 = 9000$ watts. For the calculation of the cooling of the copper in the slots it is generally convenient to take the total loss in 1 metre length of coil: this is equal to $0{\cdot}000328 \times 1{\cdot}2 \times 206 \times 206 \times 4 = 67$ watts per metre. The surface presented by the insulation works out to 1050 sq. cms. per metre length, giving 0·064 watt per sq. cm.

In order to find out whether the insulation can conduct heat at the rate of 0·064 watt per sq. cm. with a reasonable difference of temperature between the copper and the iron, one should work out the heat conductivity of the insulating tube just as it is done in the example given on page 222. Taking the conductivity of the pressed paper and mica at 0·0012 watt per sq. cm. per degree, and the thickness of the insulation at 0·25 cm., we will have for 15° C. difference of temperature between copper and iron

$$\frac{0{\cdot}0012 \times 15}{0{\cdot}25} = 0{\cdot}072.$$

We see, therefore, that we have quite sufficient cooling surface on the insulating tube to get rid of the heat generated within the coils. The cooling of the ends of the coils depends upon the shape of the coils, the amount of space allowed between each coil, and the velocity and temperature of the air circulating around them. Usually the circumstances are too complex to permit of any calculation, but experience shows that if the individual coils are kept separate, as shown in Figs. 331 and 114, so that the air can blow in between them, the cooling conditions for the end windings are at least as good as for the parts lying in the slots.

Specification No. 1 requires that the armature coils shall be able to withstand a short circuit. In Chapter VI. we considered the forces which come into play when a machine is short circuited at full voltage. It is easy to show from the considerations there taken up that the forces on the coils of this machine are not very great; and as the average throw is only 31 cms., the coils themselves, if bound together as shown in Fig. 331, are sufficiently stiff without attachment to any framework. In the case of 25-cycle machines, however, where the throw of the coils is greater, and where the ratio of the leakage flux to the flux per pole is only half of what it is in this case, the danger to the coils is considerably greater; and it is well in big generators of this kind to brace the coils by means of special clamps, as shown in Fig. 113*b*.

Cooling of the stator. It now remains to add up all the losses occurring in the stator, the heat from which must be dissipated from its surfaces. It is usual to assume that those parts of the stator coils which project into the air will be cooled by the draught of air blown upon them, so that only that part of the I^2R armature losses which is produced in those parts of the coils lying in the slots, "the buried copper," need be taken as adding their heat to the total heat dissipated by the stator surfaces. The total "buried copper" losses are readily calculated by multiplying the watts per metre by the total length of all the slots. This gives us about 3000 watts. Adding together the loss behind the slots and the loss in the teeth, we get 13,850, which with 3000 gives us a total loss of 16,850 watts. It is now necessary to see whether the cooling conditions of the stator are sufficiently good to get rid of the loss with a temperature rise not exceeding 45° C.

There is a rough-and-ready method which is sometimes used to get a rough idea of the total amount of heat which can be dissipated by the stator. According to this method, one adds the total external surfaces of the stator to the surfaces of the ventilating ducts, only one side of each duct being counted as effective. One then allows so many watts per sq. cm., the allowance being based upon the observed temperatures of similar machines running at about the same speed. For generators and motors of the ordinary type having a peripheral speed of 6000 feet per minute, or 30 metres per second, one may usually allow 1 watt per sq. in., or 0·155 watt per sq. cm. This method, though somewhat crude, gives sufficiently good results if we have means from time to time of correcting our coefficient. Applying it in our present case, we get as the total cooling surface 87,000 cu. cms., so that the total watts dissipated for 45° C. rise lies between 16,000 and 18,000.

A more accurate method is to apply the rules given in Chapter X. in estimating the watts dissipated from the inside cylindrical surface, or gap-area, the vent-area and the outside area respectively.

Watts dissipated from gap-area. The peripheral speed of the rotor is 30 metres per second, and from formula (1) (page 230) we have

$$35^\circ \text{ C.} = \frac{333 \times \text{watts per sq. cm.}}{1+3},$$

$$\text{watts per sq. cm.} = 0{\cdot}42\,;$$

so that the gap-area can dissipate 0·42 watt per sq. cm. We have taken the difference in temperature between the iron and the air in the air-gap at 35° C. This allows 10° margin for the heating up of the air in the gap. On a total area of 14,000 sq. cms. we get rid of 5900 watts.

Watts dissipated from vent-area. To arrive at h_v, one should know the velocity of air in the ventilating ducts. In enclosed machines with definite air channels this velocity is fairly well known. But in open machines it depends upon so many factors that it is difficult to estimate it even approximately. Where, however, we employ well-shaped vent spacers, and where we have plenty of room between the coils of the rotating field, we may take the mean velocity of air in the vents at one-tenth the peripheral speed of the rotor. In this case we may take it at 3 metres per second. This gives us $h_v = 0{\cdot}0042$ watt per sq. cm. per degree C. rise. It then becomes necessary to make a rough estimate of the difference between the mean temperature of the air in the ducts and the temperature of the iron. If we take the mean temperature rise of the air entering the ducts at 10° C., and of the air expelled from the ducts at 30° C., and taking the surface rise of the iron at about 40°, we have a mean temperature difference of 20°. Multiplying this into 0·0042, we arrive at 0·084 as the watts per sq. cm. dissipated from the ventilating ducts. Multiplying by the area of the ducts counting both sides, 75,000 sq. cms., we arrive at 6300 watts dissipated from the ducts.

Watts dissipated from the external surface. A great deal of heat is conducted from the punchings to the cast-iron frame, whence it passes by convection and radiation to surrounding objects. It is impossible to make an accurate estimate of the amount of heat lost in this manner. A simple plan, which gives sufficiently correct results in practice, is to take the total external surface which is made up of the two end-plates and the external cylindrical surface, and to multiply by the coefficient 0·15 watt per sq. cm., which is equivalent to about 1 watt per sq. in. The total external surface in this case amounts to 33,000 sq. cms. dissipating 4950 watts.

Estimating the cooling in this way, we arrive at a total figure of 17,150 watts dissipated by the stator iron surfaces for a temperature rise of 45°.

Design of the field-magnet. We now come to the design of the field-magnet. The considerations which govern the number of ampere-turns required on it in order to get the desired regulation have been dealt with on page 278.* We have seen on page 276 that there are reasons for making the pole wide at the base, while there are other reasons for making it narrow immediately below the polar horns. In order to avoid a taper pole, we may make the pole body in two parts (see

* And see "Effect of Leading and Lagging Currents on Regulation of Alternators," B. N. Westcott, *Elec. World*, 89, p. 46, 1912; "Regulation of Definite-pole Alternators," Mortensen, *Amer. Inst. E.E.*, Proc. 32, 291, 1913; "Experimental Determination of the Regulation of Alternators," Field, *Amer. Inst. E.E.*, Proc. 32, 599, 1913.

Fig. 334), each of rectangular shape, which are held together by the bolts which hold the pole to the main ring of the field-magnet. The making of the poles in two parts and the winding of the coils in two parts increases the cost of labour by a very small percentage, and enables the output of the frame to be increased by about 8 per cent.

It will be seen from Fig. 334 that this construction allows a cross-section of 540 sq. cms. at the root of the pole, while immediately under the horns of the pole the section is only 379 sq. cms. For a length of 8·3 cms. we are able to get sufficient saturation to improve the regulation of the machine without running any danger of excessive saturation at the root of the pole when the generator is working on heavy load of low-power factor.

The dimensions of the air-gap. As we have seen on page 62, the fixing of the length of air-gap depends upon a number of considerations. In the first place the air-gap must not be so small that the unbalanced magnetic pull for a small accidental displacement of the rotor is excessive. In practice it will be found that it is only on machines of very large diameter with a great number of poles that this consideration has much weight in controlling the width of the air-gap (see page 347). In machines of smaller diameter the air-gap has to be made fairly wide in order to get sufficient ampere-turns on the pole to obtain the desired regulation, and it is then found that the unbalanced magnetic pull is not excessively great. On page 278 we have given the relations which must exist between the ampere-turns on the field-magnet and the ampere-turns on the armature, in order that an alternating-current generator may possess certain regulating qualities. In the case of the 600 K.W. machine under consideration, the effective armature ampere-turns per pole are 3800. To obtain not more than 8 per cent. rise in voltage when full non-inductive load is thrown off, it will be necessary to have about 5500 ampere-turns per pole at no load, even with considerable saturation of the iron. This we know from the construction given in Fig. 310, though the final adjustment of the ampere-turns per pole can only be arrived at by a process of trial and error. It is thus found that an air-gap of 0·2 in. or 0·51 cm. will be sufficient to give the desired regulation.

The magnetic flux per pole is found from the $A_g\mathrm{B}$ by the formula,

$$\frac{A_g\mathrm{B} \times K_f}{\text{number of poles}} = \text{flux per pole.} \quad \dots\dots\dots\dots (1)$$

In this case
$$\frac{1{\cdot}46 \times 10^8 \times 0{\cdot}655}{16} = 5{\cdot}96 \times 10^6.$$

Calculation of leakage * between poles. Before we can estimate the number of ampere-turns absorbed in driving the flux along the body of the pole at no load and at full load, it is necessary to make a calculation of the leakage flux. This is best done by means of a graphic construction such as that given in Fig. 333. The procedure is as follows: First lay out a vertical line to represent to scale an imaginary line drawn along the neutral plane between the poles in a radial direction. This line in our present case will be 20 cms. long. Then draw a diagram

* The reader should also consult a paper by Dr. Pohl, *Jour. Inst. Elec. Engrs.*, vol. 52, p. 170, 1914.

which gives the distance from the iron of the pole to the neutral plane, as shown by the thin dotted line in Fig. 333. Then set off a curve, the abscissa of which gives the magnetomotive force exerted by the field-coil between the iron of the pole and the neutral line. At the root of the pole this magnetomotive force will

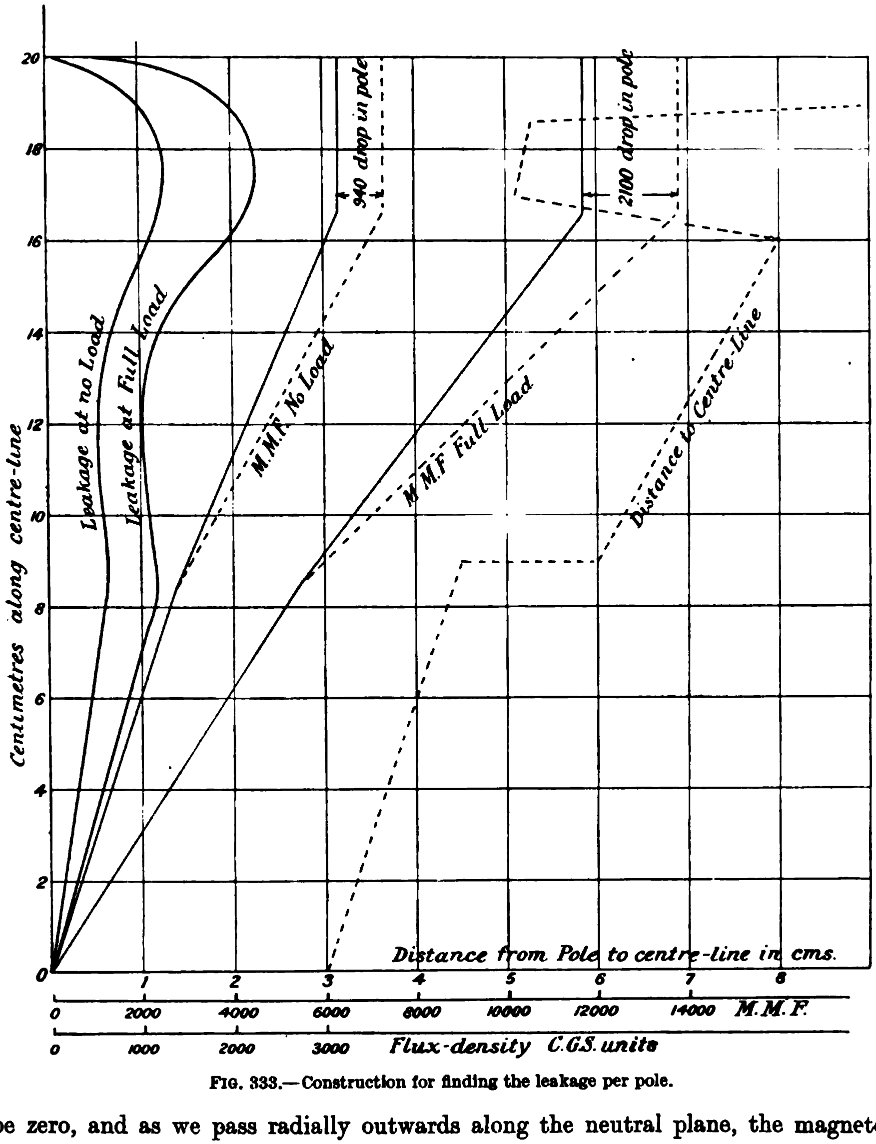


FIG. 333.—Construction for finding the leakage per pole.

be zero, and as we pass radially outwards along the neutral plane, the magnetomotive force will incréase, the rate of increase depending upon the number of ampere-turns per cm. length of pole. In those cases where the field-coil is rectangular in section, so that the ampere-turns per cm. are constant, the magnetomotive force curve is a straight line. We obtain the extreme corner of the curve by multiplying the ampere-turns per pole by 1·257, and plotting a point which has the

value thus obtained for its abscissa and a vertical height equal to the height of the coil for its ordinate. In our case the winding on the pole consists of two rectangular coils having different numbers of turns per cm., so that the magnetomotive force at no load will be represented by the dotted curve marked "M.M.F. no load," which has two straight sections of different slopes. The vertical part of the curve shows that the magnetomotive force is constant for all points on the neutral plane beyond the limits of the coil. In those cases where the number of ampere-turns absorbed by the pole itself is small, it is sufficient to use the curve of magnetomotive force yielded by the coil to obtain the flux-density between the poles. But in a case like the present, in which there is a deliberate intention to absorb a considerable fraction of the ampere-turns on the pole itself, one ought to deduct from the value given by the coil magnetomotive-force curve a certain amount for the ampere-turns lost at each point along the neutral line. For instance, in this case 750 ampere-turns ($M = 940$) are lost in the 8 cms. of pole body, so it is necessary to draw a new M.M.F. curve. This is shown by the thin full line in Fig. 333. In order to get the flux-density between the poles, it only remains to divide the effective magnetomotive force at each point by the distance from the iron to the neutral line at each point. This gives the curve shown by the thick line. This curve can be plotted fairly definitely in the upper reaches between the tips of the pole, and also in the lower reaches near the root of the pole. The middle part can be filled in by an easy-flowing curve, which we can draw by exercising some judgment upon the way that the flux would spread along the irregular path which exists between the tips of the pole and the centre of the pole. Having obtained this curve for the approximate flux-density at each point along the neutral line, we can find the mean value either by the use of a planimeter or by any other method of finding the mean height of the ordinate of a curve.

A rough-and-ready rule, which works very well in practice, for obtaining the effective area of the path between the poles is as follows: Add one-third of the pole pitch p_p to the effective axial length of the pole l_e, and multiply this by the total height of the pole h_p. For instance, in Fig. 334 we may take

$$\tfrac{1}{3}p_p = 10 \text{ cms.},$$
$$l_e = 30 \text{ cms.},$$
$$h_p = 20 \text{ cms.}$$

$20(10 + 30) = 800$ sq. cms. at each side of the pole.

If now the mean flux-density between the poles is 525 C.G.S. lines per sq. cm. at no load, the no-load leakage may be taken at

$$800 \times 2 \times 525 = 0{\cdot}84 \times 10^6 \text{ C.G.S. lines.}$$

If the mean flux-density between the poles at full inductive load is 990 C.G.S., the leakage per pole will then be $1{\cdot}58 \times 10$ lines per pole.

Adding the leakage to the no-load working flux, $5{\cdot}96 \times 10^6$, we get $6{\cdot}8 \times 10^6$ for the total flux per pole at no load, or adding $1{\cdot}58 \times 10^6$ we get $7{\cdot}54 \times 10^6$ for the flux per pole at full load.

With the type of pole illustrated in Fig. 334, we are able to carry the narrow shank immediately under the pole piece to a higher flux-density than we would risk at the root of a pole of the ordinary shape. We may safely employ a flux-

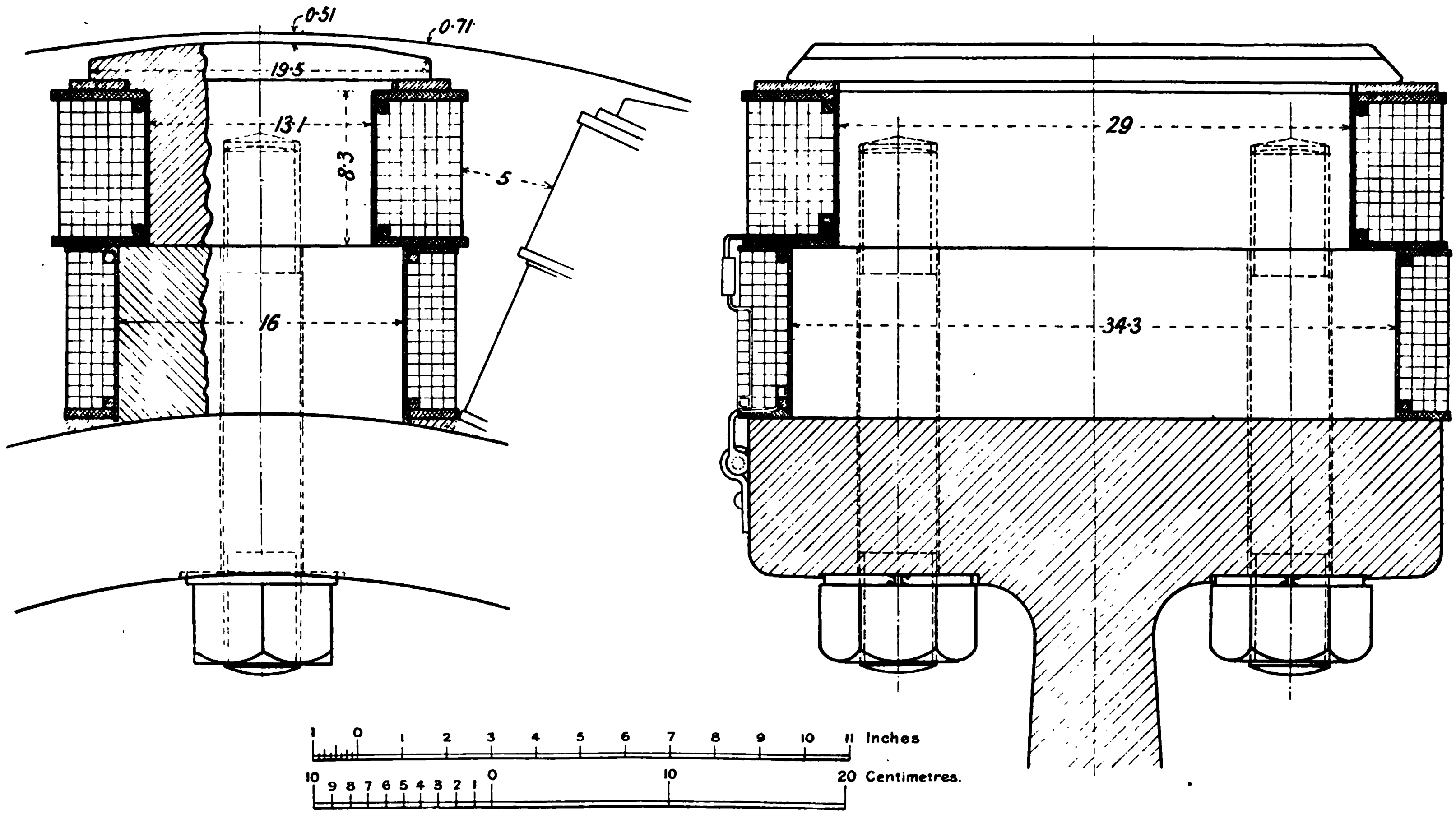


FIG. 334.—Arrangement of poles and field coils on 750 K.V.A. 3-phase generator.

density of 18,000 at no load. This would give us an area of 379 sq. cms. The flux-density at full load would then be as high as 19,800. This high saturation improves the regulating qualities of the machine.

Magnetization curve. It is usually sufficient to calculate the number of ampere-turns per pole required for three different voltages, the normal no-load voltage, another voltage some 5 per cent. higher and another voltage 10 or 15 per cent. lower. It will be found in general that from the three points in the magnetization curve thus obtained the curve can be drawn with sufficient accuracy.

Ampere-turns on the core. The number of ampere-turns on the core in 50-cycle generators is usually so small that it can be neglected in view of the errors which are sure to arise in the estimation of larger quantities. In the form given on page 321, however, the ampere-turns on the core are calculated merely to illustrate this fact.

Ampere-turns on the stator teeth. The section of all the teeth as given above is 8000 sq. cm. The length may be taken as 4·3 cms. The flux-density at 2100 volts is 18,300, and by placing the number 2·1 of the movable scale of our slide-rule opposite 18·3 on the fixed scale, we obtain $B = 19{,}200$ at 2200 volts, and $B = 15{,}700$ at 1800 volts. Referring now to our magnetization curve, suppose we obtain the figures 24, 97 and 160 for the ampere-turns per cm. in soft sheet steel at the three given densities. Multiplying by 4·3 cms. length of tooth, we arrive at 103, 415 and 690 ampere-turns on the teeth.

Ampere-turns on the gap. We have seen that the length of the air-gap has been fixed at 0·51, so as to absorb the required number of ampere-turns. The flux-density in the gap at 2100 volts is obtained by dividing $A_g B$, $1{\cdot}46 \times 10^8$, by the gap-area 15,400 sq. cms. This gives us $B = 9500$. By proportionality we get the figures 8150 and 9950 at 1800 volts and 2200 volts respectively.

Working out the gap coefficient K_g, according to the rule given on page 65, we arrive at $K_g = 1{\cdot}1$. The ampere-turns on the air-gap are therefore 3640, 4250 and 4450 at the three voltages.

Ampere-turns on the pole body. At no load the flux-densities in the pole body will be 15,900, 18,500 and 19,400 for the voltages 1800, 2100 and 2200 respectively. The length of the pole body is taken as only 8·3 cms.; that is to say, the length of the shank directly under the pole face. The ampere-turns on this part amount to 170, 200 and 210 respectively. The ampere-turns on the remainder of the magnetic circuit may be neglected. Thus the total ampere-turns per pole at no load amount to 4243, 5760 and 6760 respectively.

In plotting a magnetization curve for any particular frame, it is better to take as ordinates the flux-density in the gap than the voltage generated in the armature. It will be found in practice that many machines built on the same frame will be carried to approximately the same flux-density in the gap, whereas the voltage generated in the armature will vary widely, depending, of course, upon the number of conductors. One magnetization curve plotted with flux-density as ordinates will do for any number of machines built on the same frame. We have accordingly adopted this plan in Fig. 301.

In plotting the magnetization curve, it is well to draw first the air-gap line. We take the point which represents the flux-density of 9500 and the ampere-turns

of 4250, and draw a straight line passing through it and the origin. We then plot the points given by the no-load ampere-turns at 1800 volts, 2100 volts and 2200 volts. Thus we obtain the no-load saturation curve shown in Fig. 301. In order to obtain the dotted curve marked "Increase due to leakage on load," we must obtain at various voltages the extra ampere-turns required for the pole shank when the leakage is increased by the extra ampere-turns required on the pole at full load. The method of arriving at these extra ampere-turns will at once be understood from the table given below.

	1800 volts.			2100 volts.			2200 volts.		
	B	A.T. p. cm.	A.T.	B	A.T. p. cm.	A.T.	B	A.T. p. cm.	A.T.
Pole body (full load),	17000	50	410	19800	205	1700	20800	300	2500
Pole body (no load),			210			750			1250
Difference, - -			200			950			1250

It should be noted that when dealing with a very highly saturated pole, as in this case, some flux will be carried by the space occupied by the coil, and to find values for the actual flux-density in the pole one must roughly work out the value of K_s (see page 71), and make use of Fig. 46. In this case $K_s = 1{\cdot}5$, so that at 2200 volts an apparent flux-density of 21,000 in the pole body means an actual flux-density of 20,800.

In Fig. 301 we set off the difference 950, as shown by the line NN', and the differences for the other parts of the curve, and so obtain the increase-due-to-leakage curve.

Having obtained these two magnetization curves, it is possible to calculate with fair accuracy the ampere-turns per pole required at full load by the method described on page 293. This method simplifies down to the construction given in Fig. 310.

The calculation of the field winding. The shortest method of finding the size of wire and the number of turns required is based upon a knowledge of the number of sq. ins. or sq. cms. of coil surface required to dissipate the heat from 1 watt lost. When a designer is frequently dealing with coils of about the same size and shape, he finds from experience the amount of surface per watt to allow. For coils of about the size and shape depicted in Fig. 331, running at a speed of 5600 feet per minute, an allowance of 1·35 sq. ins. per watt will be ample, if in taking the surface we count the surface of the outside, the inside and the ends of the coils. More exact rules for determining the cooling constants of revolving field-coils are given in page 233, and a further example is worked out on page 352; but in this case we will assume that the constant, 1·35 sq. ins. per watt over the total surface, is known.

The total surface of all the coils works out at 11,400 sq. ins., or 73,500 sq. cms., so that when running at full speed we can dissipate 8500 watts.

Consider next the exciting voltage, in this case 125 volts, and allow some margin, so that even at full load we will still have some 20 per cent. or so on the rheostat. In this case we have IR in the winding 104 volts. Divide this into the 8500 watts.

We will require about 81·5 amperes exciting current, so that we will require about 129 turns per coil to get 10,500 ampere-turns. These turns may be divided between the two parts of the coil as follows: 75 turns in the upper and 54 turns in the lower. The lengths of mean turn come out 1·07 metres and 1·12 metres respectively, giving total lengths of 1280 and 970 metres respectively. The required resistance (hot) is obtained by dividing 104 by 81·5. It should come out about 1·28 ohms. From this, knowing the length of wire, we can determine the size of wire. In the machine under consideration, owing to the cooling conditions on the respective parts, we ought to make the upper part of the coil in Fig. 334 of larger wire than the lower part. It is often convenient to employ two different sizes of wire in order to hit off more exactly the desired resistance. Assuming that the resistance (cold) of the two parts of the coil will be 0·55 and 0·51 ohm respectively, we get the size of square wire 0·64 and 0·58 cm. respectively. Although the calculation is here given as a direct process (as indeed it might be if all sizes of wire were available), in practice a little adjustment of the figures by trial and error is required in order to make them fit the standard sizes of wire.

Calculation of the efficiency. The various losses in the machine are tabulated in the left-hand bottom corner of the calculation sheet. In the case of a generator direct connected to an engine, it is usual to include in the friction losses only the losses in the outboard bearing. The amount to allow for friction and windage is best obtained from measurements in similar machines. It is useful to plot the results of tests in curves like those given in Fig. 222, so that we can quickly make rough estimates of the friction and windage of rotating parts. The amount of windage will depend very greatly on the shape of the rotating parts. Any projections which act as blowers will greatly increase it, so that some judgment must be employed in using Fig. 222, which gives the friction and windage on rotating fields of the ordinary sort which are not fitted with special blowers.

The iron loss at no load has been previously found to be 13,800 watts. Where the performance specification is worded as Specification No. 1, Clause 16, the no-load iron loss is to be taken in calculating the efficiency. The full-load iron loss is so difficult to measure, that the above method of giving the guarantees is to be preferred. The field losses should include the rheostat losses, and therefore are obtained by multiplying the field current by the exciting voltage. Adding together the losses and taking the ratio of output to input, we get the efficiency figures as given on the calculation sheet.

Wave-form of E.M.F. If we plot the field-form in the manner described on page 14, we will find that the 5th harmonic (see page 22) is about $-0·09$ of the fundamental. Referring now to Table XVI. page 313, we find that the value of f_5 for an armature winding, having three slots per phase per pole, is 0·217 for the phase pressure and $0·217 \times \cos 150°$, or 0·187 for the terminal pressure. The value of the 5th harmonic in the E.M.F. wave-form is therefore $-0·09 \times 0·187 = 0·0169$. Similarly, it will be found that the value of the 7th harmonic is 0·012. The 3rd harmonic is, of course, zero, because of the star connection of the armature. As the value of f_1 is $·966 \times ·866 = ·835$, the amplitude of the 5th harmonic will be $0·0169 \div ·835 = ·02$ of the fundamental. The 7th is $0·012 \div ·835 = ·0144$.

CHAPTER XIII.

ALTERNATING-CURRENT GENERATORS (*continued*).

SLOW-SPEED ENGINE TYPE.

SPECIFICATION No. 2.

2180 K.V.A. THREE-PHASE GENERATOR TO BE DIRECT-CONNECTED TO A GAS-ENGINE.

21. The work covered by this specification is to be carried out in accordance with the General Conditions, a copy of which is attached hereto and marked A. **General Conditions.**

22. The work includes the supply, delivery, erection, etc., etc. **Extent of Work.**

(See Clauses 2, 31, 51 and 80.)

Characteristics of Generator.

Normal output	2180 K.V.A. or 1750 K.W.
Power factor of load	0·8.
Number of phases	3.
Normal voltage	6300.
Voltage variation	6200 to 6500.
Amperes per phase	200.
Speed	125 revs. per minute.
Frequency	50 cycles per second.
Regulation	7 per cent. rise with non-inductive load thrown off, the speed and excitation being constant. 20 per cent. rise with 0·8 power factor load thrown off.
Over load	250 amperes at 6400 volts with power factor between 0·8 and unity.

Exciting voltage	125.
Temperature rise after 6 hours full load	45° C. by thermometer. 55° C. by resistance.
Temperature rise after 2 hours over load	55° C. by thermometer. 65° C. by resistance.

Nature of Load. 23. The generator is intended to run in parallel with five generators of similar output and speed installed or to be installed in the same power-house. These generators will deliver a general electric supply to the town of , the load consisting partly of lighting and traction and partly of induction motors in factories. The electric tramways in the town are supplied with continuous current at 550 volts, by means of 50-cycle rotary converters fed from transformers connected to the town 6300 volt supply. The generator must be suitable in every way for this class of load.

Flywheel type or attached flywheel. 24. The generator may either be of the flywheel type, or it may have a flywheel rigidly attached to the revolving field-magnet. In the latter case, the flywheel will be considered part of the generator, and must be included in the price quoted. The construction of the flywheel and the method of attachment must be indicated in the outline supplied with the tender.

Shaft. 24*a*. The shaft will be supplied by the maker of the gas-engine, and the Contractor shall 4 weeks before the date fixed for delivery furnish the maker of the shaft with all suitable gauges and information to enable him to turn the shaft to the right diameter.

Parallel running. 25. The Contractor shall be responsible for the provision of a flywheel of the proper moment of inertia to enable the generator to run in parallel with existing sets. The following particulars are supplied to enable him to arrive at the best dimensions of flywheel:

(*a*) The gas-engine will have eight single-acting cylinders working on an Otto cycle, there being four impulses given by the engine per revolution.

(*b*) The speed is governed by controlling the amount of gas and air admitted, and not by the hit-and-miss method.

(*c*) A flywheel having a moment of inertia equal to 1500 tons at a foot radius will be sufficient to reduce the angular irregularity to 1 in 250.

The Contractor may inspect the two generators and engines already installed, and may take tachograph records at his own expense. The gas-engine to be installed will be of the same kind as those already installed, but no guarantee (other than what may be contained in the above particulars) can be given that it will operate in exactly the same manner as the present engines.

The two generator sets at present installed run in parallel. The interchange of power between the sets does not exceed 10 per cent. of the full load of one of them.

Other Clauses. Here may follow the Clauses Nos. 5, 6, 8 or its equivalent (see Clauses 55 to 59, 60 and 273), 10, 11, 12, 13, 14, 15, 16, 17, 18, 19 and 20. Instead of 19 the following clauses may be inserted:

Tests before Shipment. 26. The following tests shall be carried out before the generator leaves the Contractor's works:

Puncture test. (*a*) As many coils as possible shall be inserted and connected in the two halves of the stator frame, and the whole shall be subjected to a test pressure of 13,000 volts to earth for one minute.

Coil tested to destruction. (*b*) Any one coil may be chosen by the purchaser for testing to destruction. It shall withstand a puncture test of 3000 volts between successive turns and 16,000 volts to earth applied for 5 seconds.

Tests after Erection. 27. The following tests shall be carried out after the generator is erected on site:

Temperature Test. (*c*) The generator shall be run at full load, 0·8 power factor, for six hours, and for two hours on the stated overload, and measurements shall be taken of the temperature of the armature windings and iron and of the field windings by thermometer, and of the field windings by resistance, to see that the specified temperature rises above the surrounding air are not exceeded. For the purpose of these tests the temperature of the engine-room shall be taken three feet away from the generator, in a line with the shaft.

Puncture Test. (*d*) Immediately after the temperature run and while the machine is still warm, an alternating pressure of 13,000 volts virtual shall be applied between the armature winding and frame for one minute, and an alternating pressure of 1000 volts virtual between the field winding and frame for one minute.

Exciting Current. (*e*) A measurement shall be made of the exciting current at 6300 volts at full load at unity power factor and at 0·8 power factor.

Magnetization Curve.

(*f*) The generator shall be run at full speed at no load with the field excited, and measurements shall be taken to find the field current required at various voltages.

Short-circuit Characteristic.

(*g*) The generator shall then be run with the armature short circuited, and measurements taken to show the relation between the field current and the armature current.

Regulation.

(*h*) The regulation shall be determined by noting the current required at full load as prescribed in test (*e*), and seeing what voltage corresponds to that exciting current at no load, according to test (*f*). For this purpose 6300 volts shall be taken as the full-load voltage.

Parallel Running.

(*i*) The generator shall be synchronized and switched in parallel with the bus-bars, while these are fed by the two existing generator sets (which shall at the time be running well in parallel between themselves). The new generator shall run well in parallel on the bus-bars at all loads and at any voltage between 6200 and 6500, whether there shall be two or one of the existing sets running, and whether these or either of them shall be loaded or unloaded. The new set shall not be deemed to run well in parallel if a dead-beat wattmeter placed in circuit with it shall show an interchange of power of more than 200 K.W., after due time has been allowed for any irregularity due to switching to settle down.

Method of determining the efficiency.

(*j*) If any dispute shall arise as to the efficiency of the generator, it shall be determined in the following manner: The connecting rods shall be disconnected from the cranks of the engine, and the generator shall be run as a synchronous motor, being started up from rest with one of the other generators in the station. When running at full speed at 6600 volts unity power factor, the power taken to drive it shall be measured by means of wattmeters. The power so measured, after deducting 10 K.W. for the loss caused by the shaft and cranks, shall be taken as the iron loss, friction and windage. The I^2R loss in the armature shall be calculated from the resistance of armature taken at 60° C. The excitation losses shall be taken as the exciting current determined under (*e*), multiplied by 125. The efficiency shall be calculated from the separate losses found as above. The cost of making the iron loss test shall be borne by the party calling for the test, unless he can show that he was justified in doing so, in which case it shall be borne by the other party.

THE DESIGN OF A 2180 K.V.A. THREE-PHRASE GENERATOR, TO BE DRIVEN BY A GAS-ENGINE.

The principles which enter into the design of this machine are in general the same as those which control the design of the smaller engine-type machine, but the fixing of the flywheel effect in this case is a matter of considerable importance, To arrive at the best flywheel effect to give to a generator under any given circumstances, it is necessary to consider shortly the laws which govern the parallel running of synchronous machines.

PARALLEL RUNNING OF ALTERNATORS.

It is not within the province of this book to enter fully into the theory of the parallel running of alternators. The matter is very fully dealt with in text-books and in papers read before various institutions.*

We shall look into the matter with two main objects in view: (1) to enquire what information should be given by the man who is drawing up the specification of an alternator which is intended to run in parallel with other machines, and (2) to see what steps the designer should take to make sure that the alternator will run well in parallel under the stated conditions.

For these purposes, it is well to remind the reader of the main principles involved, and to collect the formulae to be used in a handy form.

Every synchronous alternator or motor when running in parallel with a network is constrained to run in the true synchronous position by a moment which behaves like the torque exerted by a spring; that is to say, the turning moment is proportional to the amount of displacement from the true synchronous position. If any displacement from the synchronous position suddenly occurs due to any outside disturbance, the field-magnet of the alternator swings about the central

* See Gisbert Kapp, *Elektrotechnische Zeitschrift*, vol. 20, p. 134 (1899); Goldschmidt, *ibid.*, vol. 23, p. 980, 1902; Hobart and Punga, *Trans. Am. I.E.E.*, vol. 23, p. 291, 1904; Punga, *Elektrotechnische Zeitschrift*, vol. 32, p. 385, 1911; Schüler, *ibid.*, vol. 32, p. 1199, 1911; Rezelman, *Lumière Electrique*, vol. 15, p. 67, 1911; Paper by E. Rosenberg, *Jour. Inst. Elec. Eng.*, vol. 42, p. 524; *The Dynamo*, by Hawkins and Wallis, vol. 2, p. 998; *Wechselstrommaschinen*, by W. Petersen, pp. 248 (published by Enke, Stuttgart); A. R. Everest, "Some Factors in the Parallel Operation of Alternators," *Jour. Inst. E.E.*, vol. 50, p. 520, 1913.

The following articles are also of importance:

"Parallel Operation of Alternators," G. Benischke, *Elektrotech. u. Maschinenbau*, 25, p. 1009, 1907; L. Fleischmann, *Elektrotech. u. Maschinenbau*, 26, p. 329, 1908; H. Görges, *Phys. Zeitschr.*, 9, p. 265, 1908; O. Weisshaar, *Elektrotech. u. Maschinenbau*, 26, p. 555, 1908; G. H. Shepard. *Elec. World*, 52, p. 271, 1909; "Ready Reckoner for Flywheel Effect in Armatures, etc.," H. Luckin, *Electrician*, 62, p. 642, 1909; "Parallel Operation of Three-phase Generators with their Neutrals Interconnected," G. J. Rhodes, *Amer. I.E.E.*, Proc. 29, p. 639, 1910; J. R. Barr, *I.E.E. Journ.*, 47, p. 276, 1911; "Measurement of Relative Angular Displacement in Synchronous Machines," W. W. Firth, *I.E.E. Journ.*, 46, p. 728, 1911; "Investigation of the Swinging of Synchronous Motors," Feldmann & Nobel, *Archiv f. Elektrot.*, 1, p. 291, 1912; "Apparatus for Measuring Irregularities in Speed of an Alternator," Boucherot, *Soc. Int. Elect.*, Bull. 2, Ser. 3, p. 557, 1912; "Influence of Torsional Oscillations of Shafts on Parallel Running of Alternators," L. Fleischmann, *Elektrotech. Zeitschr.*, 33, p. 610, 1912; "Bipolar Diagram of Synchronous Alternators and Motors," Blondel, *Comptes Rendus*, 156, p. 545, 1913; "The Synchronizing Couple of Synchronous Machines," Blondel, *Comptes Rendus*, 156, p. 680, 1913; "Parallel Operation of Alternators with Composite Windings," Mossman, *Elec. World*, 61, p. 56, 1913; "Phase-Swinging of two Alternators coupled by Transformers," Gavand, *Lumière Electr.*, 22, p. 103, 1913.

position like a pendulum until the energy of the swing has become dissipated. The frequency of this phase swing we will call n_s. This natural frequency of phase swing depends upon factors which we will consider later. If the natural period of the swing is the same as the period of a regularly recurring disturbance (such as may be caused by the uneven turning moment of an engine), resonance is set up, which may increase the swinging until parallel running is impossible. One of the objects of the designer will be to avoid resonance.

Let us consider a two-pole machine running in parallel with mains of constant alternating voltage and constant frequency. We can then conveniently take the phase of the voltage of the mains (sometimes called the network) to be our phase datum line, from which we can set off the angle of lag or lead of all other voltages and currents.

Fig. 335 represents the armature of an alternator connected to the supply mains. The arrow head on the circuit denotes the direction taken as positive for the

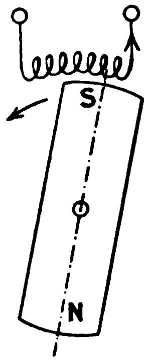


FIG. 335.

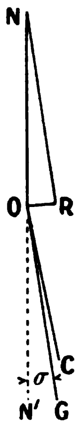


FIG. 336.

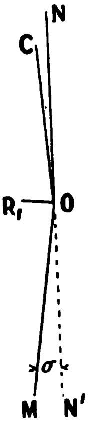


FIG. 337.

purpose of the clock diagram (Fig. 336). When the field-magnet revolves, it generates an E.M.F. in the winding which is almost directly opposed to the E.M.F. of the network. Thus the clock diagram in Fig. 336 would represent the state of affairs where voltage OG makes an angle σ with the line of the network voltage ON. The resultant voltage driving the current is given by OR and the current by OC. Some writers merely draw the triangle NOR to represent the state of affairs, but this is sufficient only if the sign of the various vectors in relation to the arrow head in Fig. 335 is clearly ascertained.

As the synchronous reactance in the generator is usually very much greater than the resistance, the current OC supplied to the mains lags about 90 degrees behind the resultant OR, and is therefore almost in phase with OG and almost 180° out of phase with ON. Under these circumstances, the machine acts as a generator, and there is a torque tending to slow it down.

If the field-magnet of the machine is behind ON', say in the position OM (Fig. 337), then the current lagging behind OR_1 will be nearly 180° out of phase with OM, so that the machine will behave as a motor. That is to say, the torque will be

such as to tend to increase the speed. This torque, called here the synchronizing torque, will be approximately proportional to the angle σ. In a two-pole machine, this is the angle which the centre line of the field poles makes with the phase datum line ON. In a machine having p pairs of poles, if α is the angular displacement of the line of the poles, then

$$\alpha p = \sigma.$$

The angle σ is the displacement on the clock diagram which shows the electrical relations, while α is the mechanical displacement. We will see later what features in a machine determine the relation between the synchronizing current and the angular displacement; but for the moment we will simply denote by I_u the synchronizing current per unit angle of displacement when the conditions are such as to keep the power factor near unity. Then for any small displacement σ the synchronizing current will be σI_u, and the synchronizing power will be $\sigma I_u E$, where E is the voltage of the network.

The synchronizing torque will be obtained by dividing this power, $EI_u\sigma$, by the speed expressed in radians per second. If R_{ps} is the speed of the generator in revolutions per second, $2\pi R_{ps}$ gives us the number of radians per second.

Thus the synchronizing torque in kilograms at a metre radius

$$= \frac{EI_u\sigma}{9{\cdot}81 \times 2\pi R_{ps}} = \frac{0{\cdot}0162 EI_u\sigma}{R_{ps}} = Q_s. \quad \text{.........................(1)}$$

Let us suppose that we have a periodic disturbance, due, say, to the irregular turning moment of the engine driving the generator, which follows the law

$$Q_d \sin 2\pi n_d t,$$

where Q_d is measured in kilograms at 1 metre radius, and let us leave out of account for the moment the synchronizing torque. The amount that the speed is changed at each pulsation will depend upon the value of the flywheel effect Σmr^2, and upon the frequency of the disturbance n_d. The increase in speed will follow the law:

$$\dot{\alpha} = -\frac{1}{2\pi n_d}\frac{9{\cdot}81 Q_d}{\Sigma mr^2}\cos 2\pi n_d t, \quad \text{..............................(2)}$$

where Σmr^2 is measured in kilograms at a metre radius^2. The amount of the angular displacement, α, of the rotor will be the integral of this, or

$$\alpha = -\frac{1}{4\pi^2 n_d{}^2}\frac{9{\cdot}81 Q_d}{\Sigma mr^2}\sin 2\pi n_d t. \quad \text{............................(3)}$$

Thus we see that the displacement is directly out of phase with the disturbing torque, and under these circumstances any synchronizing torque will be added to the disturbing torque. As the two torques are added, the phase swing will be increased. The amount of the increase will depend upon the ratio of the **synchronizing torque** (brought into action by a displacement produced by a certain disturbing torque) to the **disturbing torque**.

Let us use the symbol q for this ratio. Then

$$\frac{\text{Synchronizing torque}}{\text{Disturbing torque producing it}} = \frac{Q_s}{Q_d} = q.$$

Consider first the case when q is less than unity. Then the final value of torque will depend upon the value of the sum of an infinite series

$$1+q+q^2+q^3+q^4, \text{ etc.}$$

When q is less than unity, the sum of this series is finite and has a value $\frac{1}{1-q}$. That is to say, the ratio of final oscillating torque to the initial disturbing torque is $\frac{1}{1-q}$. Where q is less than unity, this expression gives positive values which become greater and greater as q approaches unity, and infinity when $q=1$. For values of q greater than unity the synchronizing torque is opposed to the disturbing torque, and the greater the value of q the less the displacement. The reader is referred to the very neat graphic constructions given by Dr. Rosenberg in his

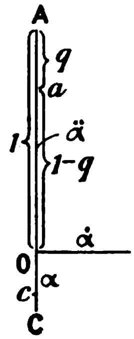


FIG. 338.

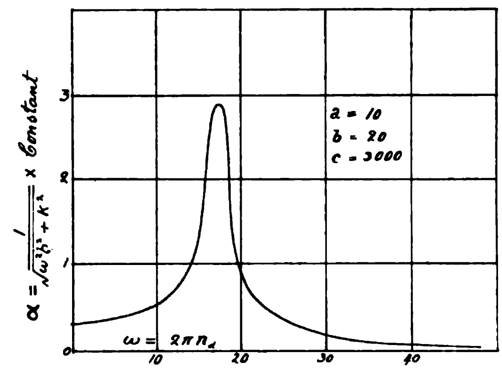


FIG. 339.—Change of amplitude of damped oscillation with change of frequency of the disturbance (see page 356).

paper, which are of great assistance in obtaining clear ideas of the relations of the various quantities involved. As $qQ_d=Q_s$, the ratio of the final synchronizing torque to the initial synchronizing torque is $\frac{q}{1-q}$. We will adopt the term "Wobble factor" there proposed for the expression $\frac{q}{1-q}$.

The relation between original disturbing torque Oa (which is taken as $1-q$) and the final torque OA (which is taken as 1) can be seen from Fig. 338, which refers to the case where $q<1$. Oc is the original displacement and OC the final displacement. Now we take our scales such that OC represents the final synchronizing torque, and add it, Aa, to the original disturbing torque Oa, getting OA. We see that if a total torque 1 produces a displacement which gives rise to a synchronizing torque q, then the ratio of the total torque to the original disturbing torque is $\frac{1}{1-q}$.

In the case of resonance we have $q=1$, and the oscillations go on increasing until they are so great that the whole of the energy of the disturbance is absorbed in the damping action of the poles. If the damping action is very small, the machine will go out of step before this point is reached. If the damping action is very great, the machine may run in parallel, notwithstanding complete resonance. The answer to the question how great the wobble factor may be before parallel running

becomes impossible, depends on the magnitude of the disturbing torque and the effectiveness of the damping action of the poles. As we change the frequency of the disturbance, keeping the other conditions constant, the amplitude of the displacement is gradually increased as we approach the frequency at which resonance occurs in the manner indicated in Fig. 339. At the crest of the curve $q=1$, and the whole of the energy of the disturbance is then expended in overcoming the damping forces (see page 602). There will in most cases be various disturbing torques, each with its own frequency. In a steam engine, even though there may be many cylinders, there is usually a disturbing torque having the frequency of the revolutions of the engine. Even though the amplitude of this torque may be smaller than the amplitude of disturbing torques having higher frequencies, it may neverthe less be the most important element to take into account, because the displacement produced is inversely proportional to the square of the frequency of the disturbance (see (3), page 339).

We have seen that the synchronizing torque

$$Q_s = \frac{0{\cdot}0162 E I_u \sigma}{R_{ps}},$$

and that the maximum value of $\sigma = \frac{1}{4\pi^2 n_d^2} \frac{9{\cdot}81 Q_d}{\Sigma m r^2} \times p.$

Therefore the maximum value of Q_s during the swing is

$$Q_s = 0{\cdot}00403 \frac{E I_u \times Q_d \times p}{R_{ps} \times n_d^2 \times \Sigma m r^2} \text{ kgs. at a metre. } \qquad (4)$$

Dividing by Q_d we get

$$q = \frac{Q_s}{Q_d} = 0{\cdot}00403 \frac{E I_u \times p}{R_{ps} \times n_d^2 \times \Sigma m r^2}. \qquad (5)$$

The critical value of flywheel effect which brings about resonance is the value which makes $q=1$. We have, therefore,

$$(\Sigma m r^2)_{\text{crit.}} = 0{\cdot}00403 \frac{E I_u \times p}{R_{ps} \times n_d^2} \text{ kilograms at a metre}^2 \text{ radius. } \qquad (6)$$

Or in British units:

$$(\Sigma m_b r_b^2)_{\text{crit.}} = 0{\cdot}0000425 \frac{E I_u \times p}{R_{ps} \times n_d^2} \text{ tons at a foot}^2 \text{ radius. } \qquad (7)$$

Or if we prefer to give the flywheel effect in kilograms on a metre **diameter**, we have

$$GD^2_{\text{crit.}} = 0{\cdot}01612 \frac{E I_u \times p}{R_{ps} \times n_d^2} \text{ kilograms on a metre diameter. } \qquad (8)$$

Observe that in the above formulae EI_u is in watts. If the synchronizing power is expressed in kilowatts, then GD^2 will be in 1000 kilograms on a metre diameter.

Let the ratio of I_u (see page 339) to the full-load current I_l be β, so that $EI_u = \beta E I_l$. We then get a simple expression for the critical flywheel effect per K.V.A. of output, as follows:

$$GD^2_{\text{crit.}} = \frac{0{\cdot}01612 \times \beta \times p}{R_{ps} \times n_d^2} \text{ in 1000 kilograms on a metre diameter per K.V.A., } \qquad (9)$$

or, in British units,

$$(\Sigma m_b r_b^2)_{\text{crit.}} = \frac{0{\cdot}0425 \times \beta \times p}{R_{ps} \times n_d^2} \text{ in tons at a foot radius per K.V.A. } \qquad (10)$$

As a first approximation, I_u is sometimes taken as the current which flows in the armature when the generator is short-circuited and run fully excited.* It has been pointed out† that this does not give a very accurate result, because on salient-pole machines the synchronous impedance when running at unity power factor is lower than when running on zero power factor. The value of I_u on a salient-pole machine will in general be higher than I_0, the short-circuit current.

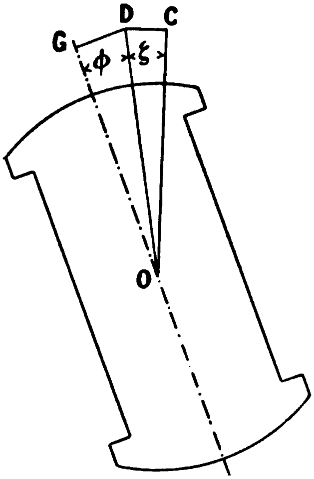


FIG. 340.

As we have seen on page 295, the angle between the centre line of the pole and the phase line of the terminal voltage E_t consists of two parts: one part due to the lag of the terminal voltage behind the generated voltage, denoted by ζ in Fig. 340, and the other due to the distortion of the field, denoted by ϕ in Fig. 340. The angle ζ can be calculated from the ratio between the true armature leakage flux and the working flux, as shown by the example given on page 345. The angle ϕ can be calculated from the ratio of the armature ampere-turns to the field ampere-turns in conjunction with the coefficient K_ϕ given in Table XVII.‡

$$\text{The distortion angle } \phi = \frac{\text{armature ampere-turns per pole (all phases)}}{\text{field ampere-turns per pole on gap and teeth}} \times K_\phi.$$

If we calculate ζ and ϕ for full-load current I_l, then

$$I_u = \frac{57 \cdot 3}{\zeta + \phi} I_l.$$

If we denote by β the ratio between the synchronizing power for $\sigma = 1$ and the normal full-load power, then

$$\beta = \frac{57 \cdot 3}{\zeta + \phi}.$$

TABLE XVII.

Ratio $\frac{\text{pole-arc}}{\text{pole-pitch}}$.	K_ϕ in degrees.
0·4	7·0
0·5	10·0
0·6	13·0
0·7	18·0
0·8	24·0
0·9	31·0
1·0	40·0

* On a three-phase machine one must, of course, multiply the current per phase by 1·73 in order to get the current I_u, which when multiplied by σE gives the synchronizing watts.

† See references given on page 337. See also communication of Mr. Shuttleworth, *Jour. Inst. Elec. Engrs.*, vol. 50, page 549.

‡ See "Some Factors in Parallel Operation," A. R. Everest, *Jour. Inst. Elec. Engrs.*, vol. 50, page 520.

The excitation that is effective in changing β is the excitation absorbed in the air-gap and teeth. This may change over a fairly wide range in the practical operation of a generator, so that there will be a fairly wide range in the value of flywheel effect that might cause resonance. For instance, for a certain generator with lowest contemplated excitation, β might be 3, and with the highest excitation

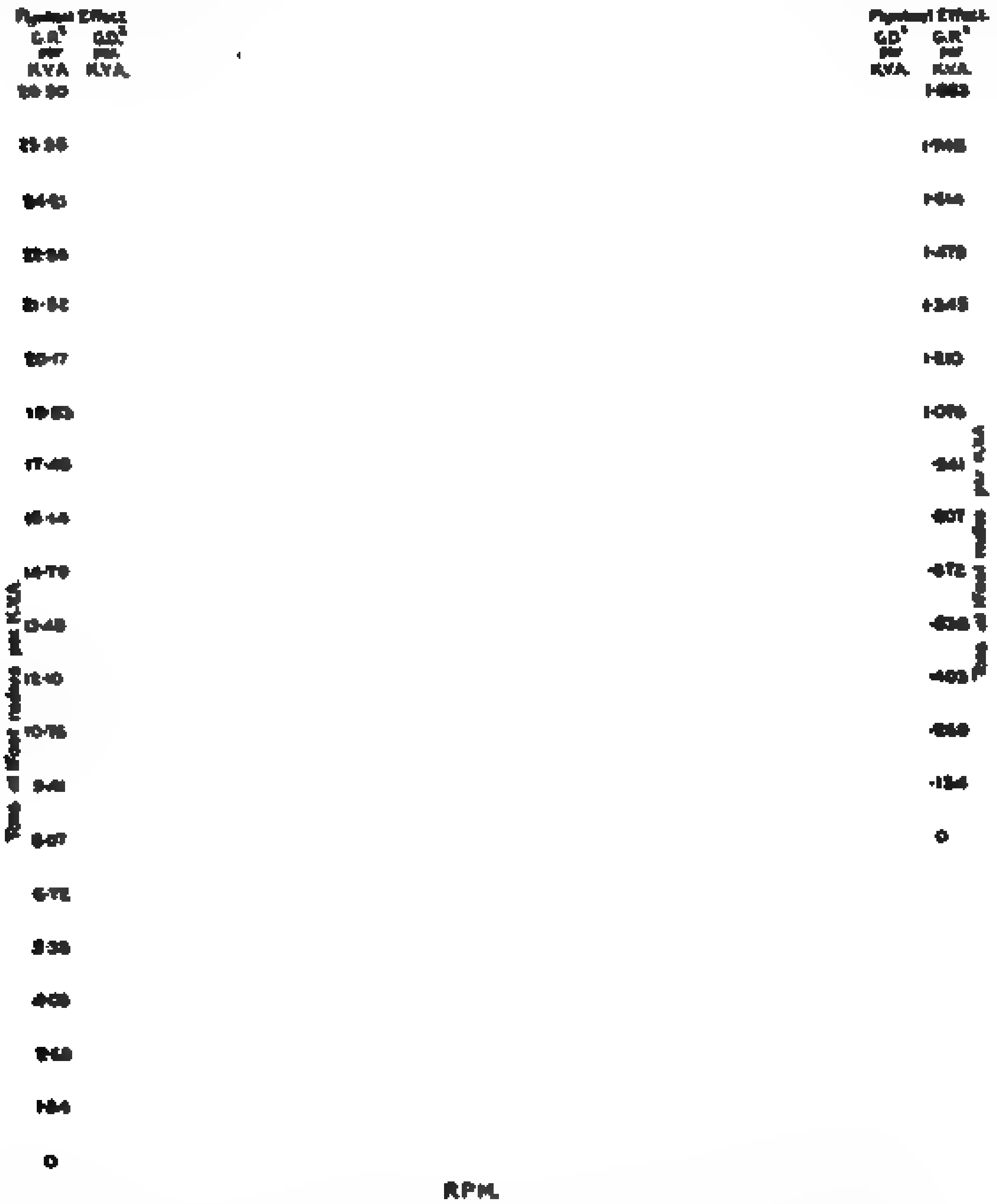


FIG. 341.

X_1 and X_2, critical values of flywheel effect for 50 periods, a short-circuit current equal to 3·5 and 4·2 times normal current, when the time of one period of oscillating torque equals the time of two revolutions.

x_1 and x_2, ditto, when the time of one period of oscillating torque equals the time of one revolution.

For any other frequency or short-circuit current, the flywheel effect will be varied in direct proportion to the frequency or short-circuit current.

I. Average flywheel effect of a *gas-engine having 4 impulses per revolution* for a cyclic irregularity $\delta = \frac{1}{160}$. II. Ditto, $\delta = \frac{1}{250}$.

it might be 3·8. Filling these two values in the formula given above, we get two values of the flywheel effect, and anywhere in between these two values there is danger of resonance, for at some excitation q would be equal to 1. Furthermore, even outside this range there are values of the flywheel effect that may make the wobble factor too great for satisfactory operation. In Dr. Rosenberg's paper referred to above, some very useful curves are given in which the resonance zones and danger zones for various types of engine and various speeds are plotted in handy form.

One of these referring to a gas engine we reproduce in Fig. 341. With a gas engine working on an Otto cycle there is always danger of some disturbing torque having a frequency of one-half of the frequency of revolution. Even if the gas engine has many cylinders, so as to give several impulses per revolution, it may happen through the setting of the valves that one of the cylinders gives a disturbing torque every two revolutions. For this reason the upper curves in Fig. 341 have been plotted by taking n_d only one-half of R_{ps}. The lower curves have been plotted by taking n_d equal to R_{ps}. Other curves might be plotted for higher harmonics, but one is not likely to be troubled with these, because the displacement of the flywheel is inversely proportional to the square of the frequency of the disturbance. It will be seen that the curves for the ordinary sizes of flywheel used with four-cylinder gas engines lie mostly in between the two danger zones, but for some speeds they are intersected by the danger zones. For these speeds it would be necessary either to make a flywheel so big as to get completely above the upper curve, or to increase the value of the short-circuit current so as to raise the curves which mark out the danger zone, or to add a damper heavy enough to ensure steady running, notwithstanding the resonance between the disturbance and the natural period of swing of the alternator.

As these curves are only plotted for certain values of β and only take into account the disturbances likely to be met with on the generator's own engine, we must have recourse to the formula

$$GD^2_{\text{crit.}} = \frac{0{\cdot}01612 \times \beta \times p}{R_{ps} \times n_d^2} 1000 \text{ kilograms at a metre diameter per K.V.A.,}$$

or

$$\Sigma m_b r_b^2{}_{\text{crit.}} = \frac{0{\cdot}0425 \times \beta \times p}{R_{ps} \times n_d^2} \text{ tons at a foot radius per K.V.A.,}$$

when designing a flywheel to avoid resonance under circumstances not covered by the curves.

In Purchaser's Specification No. 2 we have purposely made the conditions rather difficult to meet, in order to illustrate how to use the formula and adapt a machine to meet difficult conditions.

METHOD OF FIXING ON SIZE OF FLYWHEEL REQUIRED FOR AN ALTERNATOR DRIVEN BY A PRIME MOVER OF IRREGULAR TURNING MOMENT.

As an example, we will take the 2180 K.V.A. three-phase generator, particulars of which are given on page 348. This generator is to be driven by a gas-engine having four impulses per revolution, running at 125 R.P.M. under the conditions stated on page 334.

The first step is to calculate the synchronizing power for a displacement of the field-magnet of one radian behind the phase of the voltage of the network. For this purpose we first find what displacement of the field-magnet would occur at full-load unity power factor. The displacement, as we have seen on page 342, consists of two parts: the angle DOC (Fig. 340) and the angle ϕ. To calculate DOC we must make a rough estimate of the armature leakage at full-load current.

By the method described on page 422, we find that the permeance of the stator slot per cm. length of iron is 2·09. As the length of core is 34 cms., we have

$$2{\cdot}09 \times 34 \times 2 = 143.$$

At a load of 200 amperes through the four conductors per slot, the total slot leakage will therefore be

$$143 \times 200 \times 1{\cdot}41 \times 4 \times 1{\cdot}257 = 2{\cdot}04 \times 10^5.$$

To arrive at the leakage from the end windings, we take the coefficient $K_L = 2{\cdot}1$ from Table XVIII. page 427.

The end leakage at full load:

$$200\phi_e = 2{\cdot}8 \times (30 + 10) \times 2400 = 2{\cdot}66 \times 10^5.$$

Thus the full-load leakage amounts to $4{\cdot}2 \times 10^5$. This is 7·5 per cent. of the working flux per pole $5{\cdot}96 \times 10^6$ (see page 35). A leakage flux of 7·5 per cent. will make a displacement angle *DOC* of

$$\frac{7{\cdot}5}{100} \times 57{\cdot}3 = 4^\circ.$$

The angle ϕ is found as follows: Ratio of pole arc to pole pitch is $\frac{21{\cdot}5}{30} = 0{\cdot}72$, but, there being a small bevel on the pole, the effective ratio may be taken at 0·69. From Table XVII. page 342, K_ϕ will be 17·5°. The effective armature ampere-turns are 3150, and the ampere-turns on the air-gap and teeth amount to 5000. Therefore $\phi = 11^\circ$. Thus the whole angle of displacement on full-load current unity power factor is $4^\circ + 11^\circ = 15^\circ$.

If 15° gives a torque corresponding to the K.W. rating of 2180, then a displacement of one radian, or 57·3°, will give a torque 3·8 times as great. Thus β in formula (10), page 341, $= 3{\cdot}8$.

The next point to decide is, what is the natural period of oscillation at which we should aim, in order to avoid resonance? If the generator is driven by a gas-engine, one of the frequencies at which resonance might occur is the frequency of the camshaft, which runs at 62·5 R.P.M., giving $n_d = 1{\cdot}04$. We find that if we try to make the natural frequency of oscillation of the generator n_s, as low as 80 per cent. of this, we shall require a flywheel of enormous dimensions, which will be very costly, and will greatly increase the friction of the bearings. Moreover, this heavy flywheel would be very much greater than is necessary to reduce to a workable amount the cyclic irregularity of a gas-engine having four impulses per revolution.

We will therefore try whether a smaller flywheel, giving a natural frequency of oscillation greater than 62·5 per minute, will do. We must remember that we must not make the flywheel too small, or there will be danger of resonating with the frequency of revolution 125 per minute. We will therefore aim at a natural frequency of oscillation of about 90 per minute, so as to come well between 62·5 and 125. 90 per minute gives us $n_s = 1{\cdot}5$. In formula (10), page 341, we have the required flywheel effect

$$= \frac{0{\cdot}0425 \times 3{\cdot}8 \times 24}{2{\cdot}08 \times 1{\cdot}5 \times 1{\cdot}5} = 0{\cdot}82 \text{ ton at a foot radius per K.V.A.};$$

that is to say, 1800 tons at a foot radius for the generator in question.

We may check this calculation from the formula given by Mr. Everest:

$$f_0 = 9{\cdot}76\sqrt{\frac{2180 \times 3{\cdot}8 \times 50}{\text{foot-tons}}}.$$

Taking f_0 at 90, this gives us 4800 foot-tons of stored energy, at a speed of 125 R.P.M. A flywheel effect of 1800 tons at a foot radius at a speed of 2·08 per second gives us:

$$\frac{1}{2}\,\frac{1800 \times 4\pi^2 \times 2{\cdot}08^2}{32{\cdot}2} = 4800.$$

We have given the calculation here at length, in order that the reader may understand the method. It would, of course, have been very much shorter to refer at once to Dr. Rosenberg's curves given in Fig. 341. These curves refer to a machine in which the synchronizing torque for one radian of displacement lies between 3·5 and 4·2 times the synchronising torque at full load, and may therefore cover the case where $\beta = 3{\cdot}8$. It will be seen from these curves that, if it is desired to get completely above the curve Y_2 when the speed is 125 R.P.M., a flywheel effect of 2·7 tons at a foot radius per K.V.A. will be required. This would

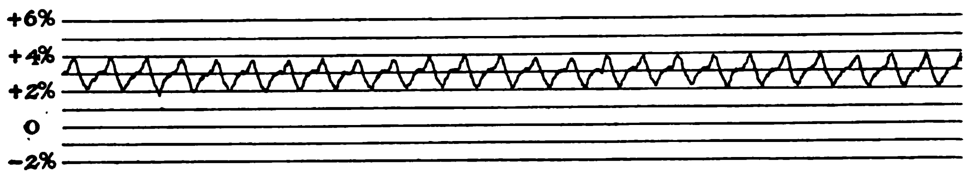


FIG. 342.

call for a flywheel of 5900 tons at a foot radius. If, however, we content ourselves with getting in between the curves Y_1 and y_2, a flywheel of about 1800 tons at a foot radius will suffice. It will further be seen from the dotted curves *I* and *II* that a flywheel of the size chosen is satisfactory from the engine-builder's point of view.

In cases where there is any doubt as to the periodicity of the disturbing torque or the amount of it, tachograph records should be taken of the engine under consideration. Sometimes there is a source of disturbance which would otherwise have been left out of account. For instance, if an engine drives a single-acting condenser pump, this may have a serious effect upon the torque diagram. Fig. 342 shows a tachograph record taken on a four-cylinder compound steam-engine running at 75 R.P.M. when running at three-quarter load with normal setting of the valves. Notwithstanding the eight impulses per revolution, it is found in this case that there is a very decided irregularity occurring once per revolution, which is much greater than any of the other disturbing causes.

Damper or amortisseur. Where a generator is driven by a gas engine, and in all cases where there is an irregular turning moment or an unsteady frequency, in addition to selecting the best flywheel effect, we should provide the machine with a suitable damper (see Fig. 346). Where the conductivity of the damper is high, it is possible to run synchronous machines in parallel notwithstanding resonance, so long as the disturbing torque is not too great. When the flywheel effect is such as to give resonance, the whole of the energy of the disturbance is expended in

overcoming the forces set up by the damper, and the amount of the phase-swing is just sufficient to call into being damping forces great enough to balance the forces creating the disturbance. This matter is treated quantitatively on pages 352 and 601.

THE DESIGN OF THE 2180 K.V.A. GENERATOR TO MEET SPECIFICATION No. 2 (PAGE 332).

Having fixed upon the approximate flywheel effect which should be given to the revolving part in order to avoid resonance, we have to decide whether we will put the whole of the flywheel effect into the magnet wheel itself, or whether we will provide a separate flywheel. It will be found that for slow-speed generators of output less than 3000 K.W. it is more economical to provide a separate flywheel. The economical diameter for the generator is generally too small to give it much moment of inertia, and therefore if we try to get the desired flywheel effect into the magnet wheel, we must either add an enormous weight to a magnet of reasonable diameter, or we must increase the diameter so much as to greatly add to the cost of the armature surrounding it.

We will, therefore, in this case fix upon the diameter of the field-magnet from the considerations which are set out on page 299, and add a flywheel to give the desired moment of inertia. It must be remembered that in any case the magnet wheel will be too great to be shipped in one piece, so that it will have to be constructed in parts connected together by links or bolts.

If we had a frame with a bore of stator of 464 cms., that would be quite suitable; but the diameter might be changed over a fair range without appreciably affecting the cost of the generator. To get the approximate length we might take the D^2l constant at about 4×10^5. This gives us l about 32·5 cms.

It is found that with generators of this diameter there is very seldom any trouble in meeting the regulation guarantees. The air-gap must be made of reasonable length to avoid "pulling over," due to accidental unbalanced magnetic pull, and if we have a reasonably great flux-density in the gap, so as to use our material well, it will be found that the ampere-turns per pole are sufficient to give us even better regulation than that called for in Specification No. 2.

One, therefore, begins the calculation of a machine of this size with a rough calculation of the magnetic pull. We would like a flux-density in the gap of about 9000 C.G.S. lines per sq. cm. There will be 48 poles to give 50 cycles at 125 R.P.M. Allowing a pole arc 0·64 of the pole pitch on the diameter chosen, each pole will have an area of about 600 sq. cms. It would not be well on a machine of this size to have an unbalanced pull greater than 5000 kilograms actual, or say 15,000 kilograms, neglecting saturation, for one millimetre displacement; so, from the formula on the top of page 60, we get:

$$g \text{ in mm.} = \frac{4{\cdot}05 \times 10^{-8} \times 9000^2 \times 48 \times 600}{15{,}000},$$

$$g = 6{\cdot}3 \text{ mm.}$$

Let us take g at 0·65 cm. and B at 9000, and see about how many ampere-turns we will want on the air-gap:

$$0{\cdot}65 \times 9000 \times 0{\cdot}796 = 4650 \text{ A.T.}$$

This, as we shall see later, is sufficient to give us the required regulation.

Date 15 June 1913 Type G.E. A.C. ~~GEN.~~ SYN MOTOR ~~ROTARY~~ 48 Poles ...... Elec Spec. 2
K.V.A. 2180; P.F. .8; Phase 3; Volts 6300; Amps per ter. 200; Cycles 50; R.P.M. 125; ~~Rotor Amps.~~
~~H.P.~~ ...... Amps p. cond. 200 Amps p. br. arm. ...... Temp. rise 45°C. Regulation 8% P.F.=1. Overload 25% contin.

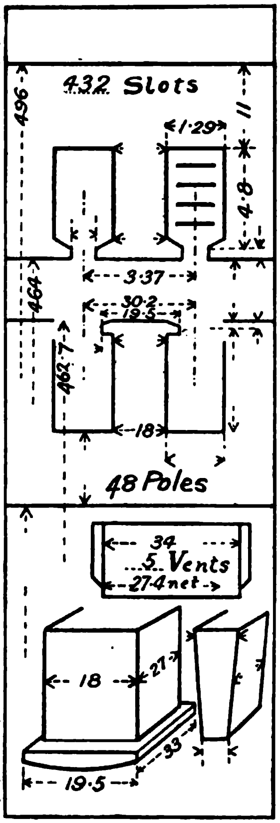
Customer Ajax Gas Engine Co.; Order No. ......; Quot. No. ......; Perf. Spec. ......, Fly-wheel effect 1800 kgs m²

Frame 496/34 Air ...... Circum 1455; Gap Area 49500; poss. $A_g B$ ......; poss. $I_a Z_a$ ......; $I_a Z_a$ Circum. 238; $A_g B$ 4.6 × 10⁸; $I_a Z_a$ 346,000; $\frac{D^2 L \times RPM}{K.V.A.} = 4.2 \times 10^5$

K_a .4 ; 6600 Volts = .4 × 2.08 × 1728 × 4.6 ; Arm. A.T. p. pole 3150 Max. Fld. A.T. 10,000

Armature. ~~Rev.~~ Stat.

Core.	Dia. Outs.	496	
	Dia. Ins.	464	
	Gross Length	34	
	Air Vents	5, 065	
	Opening Min. Mean		
	Air Velocity	30 m. per sec.	
	Net Length 30.75 × .89	27.4	
	Depth b. Slots	11	
	Section 300 Vol.	4.46×10⁵	
	Flux Density	11,100	
	Loss .054 p. cu. cm. Total	24,000	
	Buried Cu. 9000 Total	33,000	53,000
	Gap Area 49,500 Wts	21,000	
	Vent Area 242,000 Wts	20,400	
	Outs. Area 89,000 Wts	13,300	54,700
Teeth.	No of Segs 24 Mn. Circ.	1476	
	No of Slots 432 × 1.29 =	556	
	K_a	920	
	Section Teeth	25,200	
	Volume Teeth	125,000	
	Flux Density	18,200	
	Loss 0.16 p. cu. cm. Total	20,000	
	Weight of Iron	4470	kgs.
Conductors.	Star ~~or Mesh~~ Throw	1-12; 2-11; 3-10	
	Cond. p. Slot	4	
	Total Conds	1728	
	Size of Cond. .65 × .85	0.52 sq. cm.	
	Amp. p. sq. cm.	385	
	Length in Slots 34		
	Length outside 60 Sum	94	
	Total Length	1620	
	Wt. of 1,000 m. 464 Total	750	
	Res. p. 1,000 .328 Total	.54	
	Watts p. m.	63	
	Surface p. m.	1100	
	Watts p. Sq. cm.	.057	
	.26 × .057 = .0012	12°C	



Field ~~Stat. or~~ Rotor.

Dia. ~~Bore~~	462.7		
½ Total Air Gap	.65		
Gap Co-eff. K_g	1.04		
Pole Pitch 30.2 Pole Arc	19.5		
K_f	.65		
Flux per Pole	6.23×10⁶		
Leakage n.l. 1.6 f.l. 2.4	8.63×10⁶		
Area 480 Flux density	18,000		
Unbalanced Pull	6,500 kilogrs.		
No. of Seg. Mn. Circ.			
No. of Slots × =			
Vents			
K_s Section			
Weight of Iron poles	4100 kilog.		
	Shunt.	**Series.**	**Comm.**
A.T. p Pole n. Load	5380		
A.T. p. Pole f. Load	9500	say 10,000	
Surface Ends	46,600	48,000	95,000 core
~~Surface p.~~ Watts	9400	1920	13,800
I² R	21.6 K.W.		
I R	98		
Amps.	222		
No. of Turns	45		
Mean l. Turn	109		
Total Length	2160 m		
Resistance	.38 ohms cold	.44 hot	
Res. per 1,000	.17		
Size of Cond.	0.3 × 3.5	= 1 sq. cm.	
Conds. per Slot			
Total			
Length			
Wt. per 1,000 m.	890		
Total Wt.	1920		
Watts per Sq. cm.	.114		
Star or Mesh			
Paths in parallel			

Magnetization Curve.

	Section.	Length	5800 Volts. B.	A.T. p. cm.	A.T.	6300 Volts. B.	A.T. p. cm.	A.T.	6600 Volts. B.	A.T. p. cm.	A.T.
Core											
Stator Teeth	25500	4.8	15,800	30	145	17,200	70	340	18,200	125	600
Rotor Teeth											
Gap	49,500	.65	8150		4400	8850		4800	9300		5050
Pole Body F. L.	480	8	15800	30	240	17,100	70	560	18,000	115	920
~~Yoke~~					4785			5700			6570
Pole Body N. L.					120			240			400
					4665			5380			6050

Commutator.

Dia. ...... Speed ......
Bars
Volts p. Bar
Brs. p. Arm
Size of Brs.
Amps p. sq.
Brush Loss
Watts p. Sq.

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.	18	18	18	18	18
Iron Loss	43	43	43	43	43
Field Loss	24	21.6	19	17	15
Arm. &c. I²R	40	25.4	14	6	1.5
Brush Loss					
	125	108.0	94	84	77.5
Output	2180	1750	1310	875	438
Input	2305	1858	1404	959	515
Efficiency %	92.5	93.4	93.4	91.2	85

Mag. Cur ÷ Loss Cur.
Perm. Stat. Slot 2.1
„ Rot. Slot × =
„ Zig-zag
2 × 34 × 2.1 = 143 =
1.77 × 200 × 4 × 143 = 2.04
End 2.1 × 42.5 × 2400 = 2.16
Amps; Tot. 4.2×10⁶
÷ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

Leakage = 4.2 = 7.1 % / 59.6 = 450 v.

We have a provisional figure for A_g, namely $464 \times \pi \times 32{\cdot}5$, and this gives us $A_g\text{B} = 4{\cdot}25 \times 10^8$, from which we get the approximate $Z_a = 1870$. Now we would like the number of conductors to be divisible by 3×48. As $12 \times 144 = 1728$, we choose that number of conductors and work back to the correct $A_g\text{B}$. This comes out $4{\cdot}6 \times 10^8$, as shown on the calculation sheet, page 348. Drawings of the generator are given in Figs. 343, 344, 345 and 346.

The ampere-wires $I_aZ_a = 346{,}000$, and the armature A.T. per pole $= 3150$. Add to this 7·5 per cent. of 4650 (see pages 283 and 345) to get the approximate A.T. per pole on short circuit, 3500. From an assumed saturation curve, and by the aid of Fig. 312, we can judge with fair accuracy that the regulation will be well within 8 per cent. on unity power factor. Thus the above preliminary data are good enough on which to found the design.

It is unnecessary to go in detail through the calculation sheet, as the general method of working is the same as that indicated on pages 316 to 332 in connection with the 600 K.W. generator.

With our increased $A_g\text{B}$ the length of armature iron comes out at 34 cms., in order to keep the density in the teeth at 18,200. The total losses to be dissipated by the iron surfaces of the stator come out at 53,000 watts, and the watts which the frame can get rid of with a temperature rise of 45° C. come out, on a conservative estimate, at 54,700, so we expect to meet the temperature guarantee in that respect.

The size of the armature conductor is settled by the considerations given on page 323, and in this case a current density of 385 amperes per sq. cm. of copper would appear to give us a temperature rise of 12° C. above the surrounding iron.

It will be seen from the drawing that we choose a parallel pole body in this case. The number of poles being great, the angle between the neutral planes bounding a pole is very small, so that a parallel pole body and a field coil with parallel sides fill the space fairly well, leaving just nice room for ventilation.

The magnetization curve is worked out as indicated in the calculation sheet. The figures are given for ampere-turns on the pole body with the degree of saturation brought about by the leakage at full load (from which can be plotted the increase-due-to-leakage curve, see p. 331), and also for the ampere-turns on the pole body with the smaller degree of saturation brought about by the leakage at no load. From the latter figures we get the no-load magnetization curve.

It may be of interest to work out in detail the cooling conditions of the field coils of this machine, as a further example of the use of the formulae given on page 233.

The cooling coefficient for the ends of the coils :

$$h_e = 0{\cdot}0011 \times (1 + 1{\cdot}2 \times 125 \times 11{\cdot}2 \times 221 \times 10^{-5}),$$

$$h_e = 0{\cdot}00505.$$

We are allowed 45° C. rise of the field coils above the air. The coils are of copper strap on edge, so that the heat conductivity of the coil is very good. We are therefore justified in taking the temperature difference between the surface of the coil and the air at 40° C. when running. The total surface of the ends works out at 46,600 sq. cms. Therefore the heat dissipated by the ends is

$$40 \times 46{,}600 \times 0{\cdot}00505 = 9400 \text{ watts}.$$

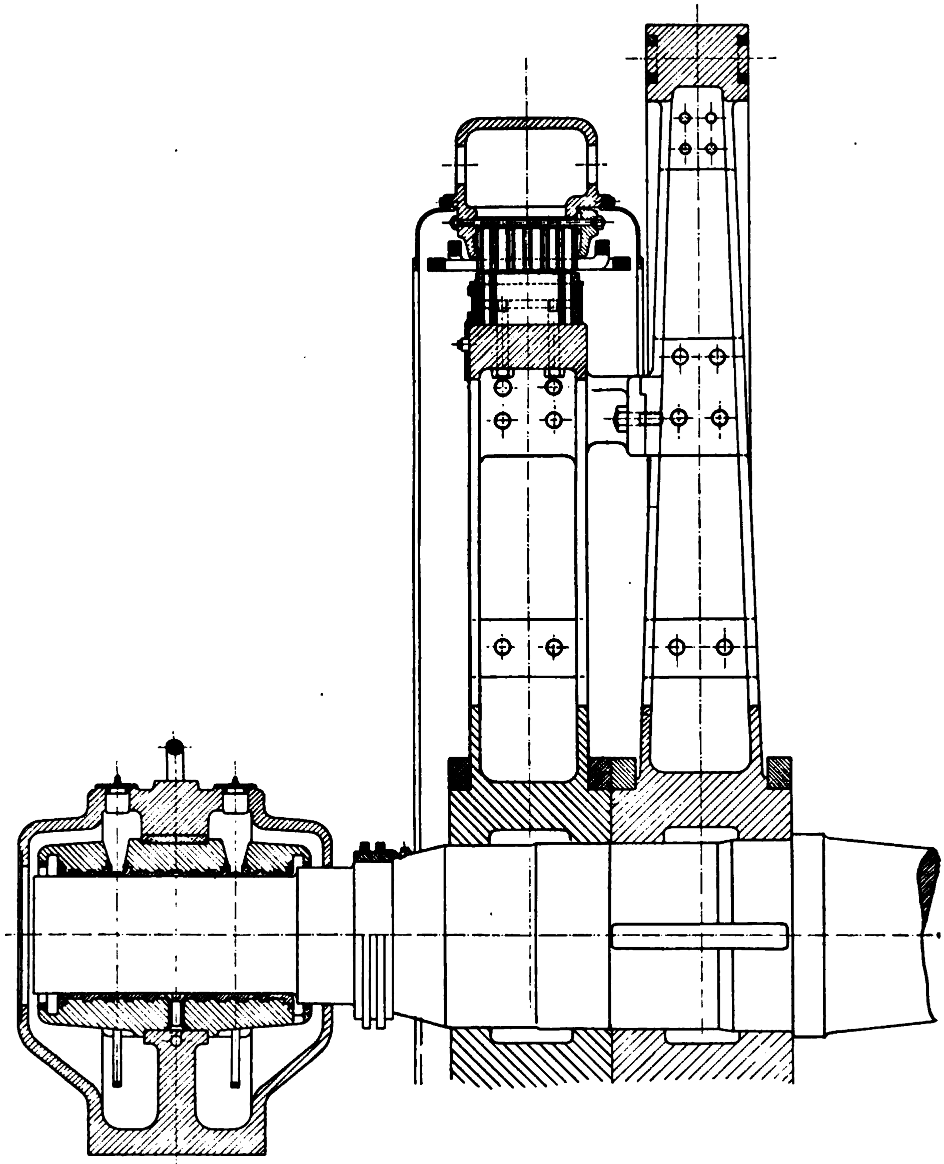


FIG. 343.—2180 K.V.A. 3-phase 50-cycle generator and flywheel, designed to be direct connected to a gas-engine running at 125 R.P.M.

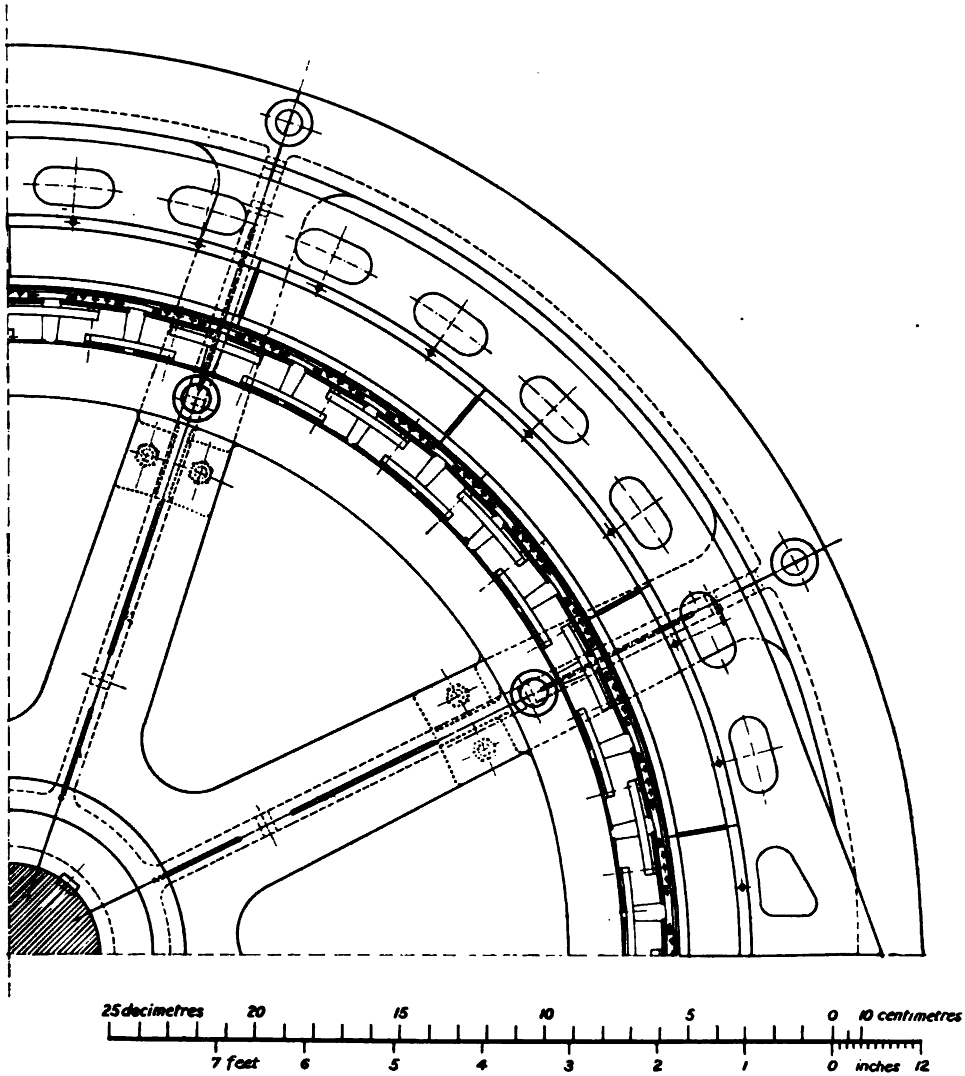


FIG. 344.—Showing flywheel consisting of eight sectors of cast steel. The centrifugal force on each sector is borne by the arms, which are bolted together and secured by shrink rings on the central boss.

The cooling coefficient for the part of the coil between the poles is

$$h_l = 1 \cdot 5 \times 10^{-8} \times 125 \times 11 \cdot 2 \times 221 \times 0 \cdot 21.$$

Here $s = 1 \cdot 3$ and $l = 28$, so that $\sqrt{\frac{s}{l}} = 0 \cdot 21,$

$$h_l = 0 \cdot 001.$$

The total surface of coils between poles is 48,000, so that the total heat dissipated from this surface is

$$40 \times 48{,}000 \times 0 \cdot 001 = 1920.$$

It is interesting to note that this is less than one quarter of the heat lost from the ends, notwithstanding the larger cooling surface.

The thickness of the insulation around the pole body is 0·25 cm., and the heat conductivity may be taken at 0·0012, allowing for some air-spaces. A pole of this kind will not rise in temperature more than 10° C. above the air when running, so we may take the mean difference in temperature between the coil and the pole as 30° C.

$$\text{Watts per sq. cm.} = \frac{0 \cdot 0012 \times 30}{0 \cdot 25} = 0 \cdot 14.$$

The total surface may be taken as 95,000 sq. cms.

$$95{,}000 \times 0 \cdot 14 = 13{,}300 \text{ watts conducted to the pole.}$$

So that the total watts dissipated for 45° C. rise are 24,620. This is a conservative estimate, as the coefficients given in the formula are "safe" coefficients. Let us now, as a matter of interest, calculate the watts dissipated in the rough manner described on page 232. If we allow 1 sq. in. per watt, or 0·155 watt per sq. cm., we have

$$0 \cdot 155 \times 189{,}600 = 29{,}400 \text{ watts,}$$

a result which is probably not far from the mark. A calculation of the actual watts lost in the coil at full load (0·8 power factor) gives us 21·6 K.W., so that the coil will be safely below 45° C. as measured by thermometer.

The figures given for the stator leakage in the bottom right-hand corner of the calculation sheet are explained on page 331.

Calculation of the effect of the damper. The simplest method of expressing the effectiveness of the damper is to regard it as the squirrel cage of the rotor of an induction motor, and to find the slip at full load which the machine would have when run as induction motor. At full load the ampere-wires on the rotor must be equal to the working ampere-wires in the stator. Now the working ampere-wires in the stator, when carrying a load of 1750 K.W., are 278,000. Let us take the damper shown in Fig. 346, consisting of three copper rods through the pole, and one rod on each side, making five rods per pole. Each of these has a cross-section of 2·6 sq. cm. There being 240 bars in all, we would have a virtual current per bar of $\frac{278{,}000}{240} = 1160$ amperes when running as an induction motor. The current in the end connections would be

$$\frac{278{,}000}{48 \times 2} \times 0 \cdot 637 = 1850 \text{ amperes virtual.}$$

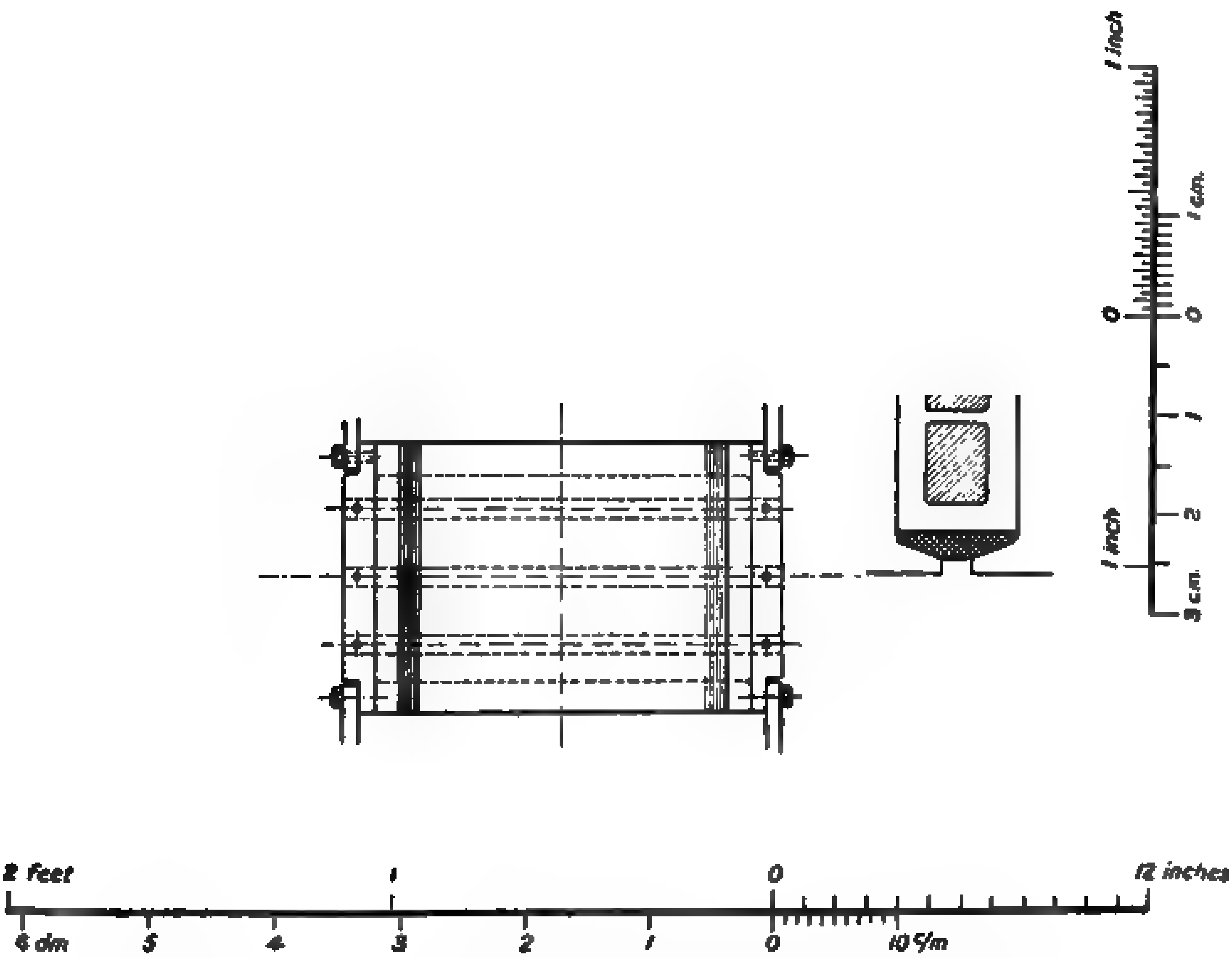


FIG. 345.—Section of 2180 K.V.A. 3-phase, 50-cycle, 6300-volt generator, designed to meet Specification No. 2, page 333.

Now the resistance of all the rods in series, allowing for joints, is about 0·006 ohm, so that the loss in all the bars would be $1160 \times 1160 \times 0{\cdot}006 = 8100$ watts. It is impossible to calculate exactly the resistance of the end connectors (or end rings), because the resistance of the joints is such an uncertain quantity. If the joints are well made, one may allow for them by adding 100 per cent. to the calculated resistance. The total length of copper in the two end rings is 29 metres, and it has an average cross-section of 9·6 sq. cm. The resistance of the whole in one length, without joints, would be 0·00051 ohm. Take the resistance with joints at 0·001 ohm. Then, as the virtual current flowing in these end rings is 1850 amperes, the loss in them is 3500 watts, giving a total loss of 11,600. Now the slip of an induction motor is equal to the ratio of the I^2R loss on the rotor to the total power supplied to the rotor (see page 433). Therefore the slip at full load with this damper acting as the squirrel cage of an induction motor will be

$$\frac{11{\cdot}6}{1762} = 0{\cdot}0066 \text{ (or } 0{\cdot}66 \text{ per cent.).}$$

Denote this slip by s.

The full load torque is $\dfrac{EI \times 1{\cdot}73}{9{\cdot}81 \times 2\pi R_{ps}}$ kilograms at a metre (see page 339).

This torque is obtained with a relative angular speed between rotor and the revolving stator field of $\dfrac{2\pi ns}{p}$, where n is the frequency and p the number of pairs of poles. Therefore, for an angular speed of 1 radian per second the torque will be

$$\frac{EI \times 1{\cdot}73 \times p}{9{\cdot}81 \times 2\pi \times R_{ps} \times 2\pi ns} \text{ kilograms at a metre.}$$

If $\dot{a}$ is the relative angular velocity between the rotor and the revolving field of the stator, the torque due to the damper at any instant is

$$\dot{a}\,\frac{EI \times 1{\cdot}73 \times p}{9{\cdot}81 \times 2\pi \times R_{ps} \times 2\pi ns} \text{ kilograms at a metre.}$$

We make use of this formula on page 601, where the general theory of phase-swinging under the influence of a damper is considered.

It is convenient to speak of a damper as a 1 per cent. damper or a 2 per cent. damper, according as the slip at full load would be 1 per cent. or 2 per cent. The damper worked out above would be described as a 0·66 per cent. damper.

It is interesting to enquire how far a damper such as the one illustrated in Fig. 346 would be effective in preventing excessive phase-swinging in the event of the disturbance being such as to cause resonance.

It will be seen from the theory given on page 602 and the example worked out on page 356, that the amplitude of the phase-swing when resonance occurs is such that the disturbing force is exactly balanced by the force exerted by the damper. The amplitude of the phase-swing is proportional to the disturbing force, and inversely proportional to the conductivity of the damper and the frequency n_d.

Where a damper has sufficient conductivity, it may reduce the phase-swinging to an amount which makes running quite possible even though $q = 1$. The amount

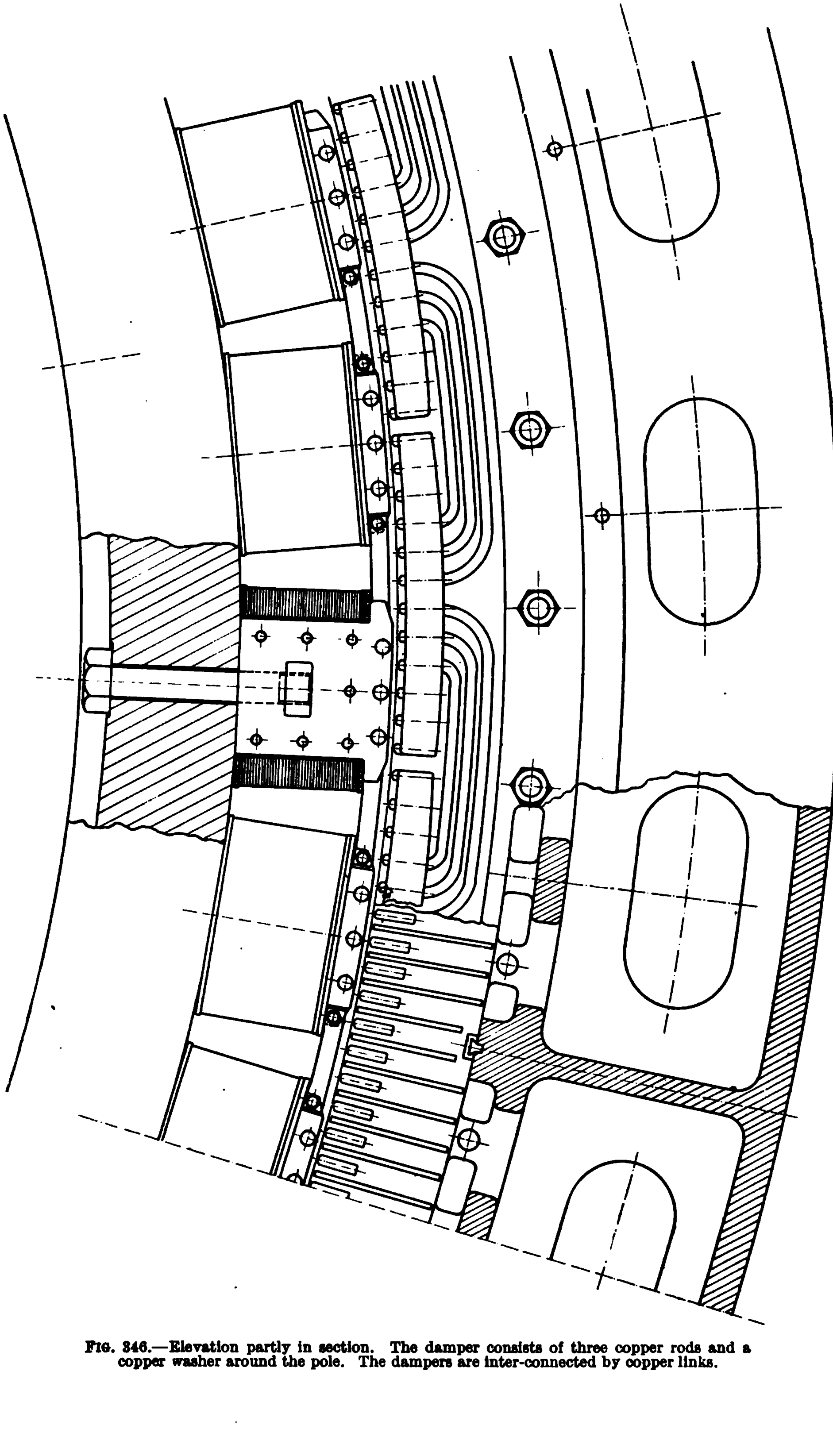


FIG. 346.—Elevation partly in section. The damper consists of three copper rods and a copper washer around the pole. The dampers are inter-connected by copper links.

of the phase-swinging produced by a given disturbing torque can be approximately calculated by the method * given on page 601.

EXAMPLE 47. In the generator worked out on page 348, having a damper like that described (0·66 per cent.), and a flywheel effect of $1{\cdot}7\times10^5$ kilograms at a metre², find the amplitude of the phase-swing a when the disturbing torque in kilograms at a metre follows the law $8500 \sin 2\pi\times1{\cdot}5\times t$. As we have here $n_d=1{\cdot}5$, $q=1$, so that a would be infinite, if it were not for the operation of the damper.

$$a=-\frac{8500\sin\left(\omega t+\frac{\pi}{2}\right)}{\omega b},$$

$$\omega=2\pi\times1{\cdot}5=9{\cdot}42,$$

$$b=\frac{1{\cdot}750\times10^6\times24}{9{\cdot}81\times2{\cdot}08\times6{\cdot}28\times6{\cdot}28\times50\times0{\cdot}0066}=1{\cdot}58\times10^5,$$

$$\omega b=1{\cdot}49\times10^6,$$

$$a=-\frac{8500}{1{\cdot}49\times10^6}\sin\left(\omega t+\frac{\pi}{2}\right)=-5{\cdot}7\times10^{-3}\sin\left(\omega t+\frac{\pi}{2}\right).$$

That is to say, the displacement lags 90° behind the disturbing torque, and has a maximum value of 0·0057 radian, equivalent on a two-pole machine to $0{\cdot}0057\times24=0{\cdot}137$ radian, or 7·8 electrical degrees.

In this case $a=\frac{1{\cdot}7\times10^5}{9{\cdot}81}$; $b=1{\cdot}58\times10^5$ and $c=1{\cdot}55\times10^6$. We therefore have $a\omega^2=c$; that is to say, the forces required for the acceleration of the flywheel are just supplied by the synchronizing forces, leaving $b\dot{a}=8500\sin\omega t.$

EFFECT OF HIGHER SPEED ON THE DESIGN.

If the speed of the generator were higher, say 150 R.P.M., and we wished to keep the same peripheral speed, we should have to reduce the diameter. Now, the output changes approximately as the square of the diameter, so the outputs of machines of the same peripheral speed and the same length will vary approximately as the diameter. In this case, if the speed specified had been 150 R.P.M., we should have been compelled either to run at a higher peripheral speed or to lengthen the frame. The best plan would be to choose a diameter of about 410 cms. and a length of 36 cms.

The way in which the number of conductors is increased on a machine of smaller output, but of the same voltage, is illustrated in the calculation sheet of the 1800 K.V.A. generator given on page 357. Here the speed is 150. The number of poles is 40, and the diameter has been reduced from that of the last machine in ratio of the number of poles. The total A_gB is reduced in the same ratio, and consequently the conductors have been increased from four per slot to five per slot.

* In the case of a generator, running in parallel with a network of constant frequency, and driven by an engine which exerts a disturbing torque, $Q_d \sin 2\pi n_d t$, the equation of motion is

$$a\ddot{a}+b\dot{a}+ca=Q_d\sin 2\pi n_d t,$$

where a, b and c have the values given on page 601. Writing $2\pi n_d=\omega$ and $(a\omega^2-c)=k$, we have

$$a=-\frac{Q_d}{\sqrt{\omega^2b^2+k^2}}\sin\left(\omega t+\tan^{-1}\frac{\omega b}{k}\right).$$

When $q=1$, $k=0$, then $a=-\frac{Q_d\sin\left(\omega t+\frac{\pi}{2}\right)}{\omega b}.$

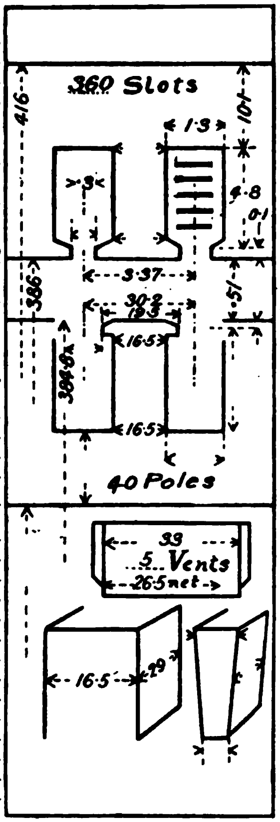
Date 5 July 1912 Type E.T. A.C. GEN. ~~SYN MOTOR ROTARY~~ 40 Poles ...... Elec Spec 3
K.V.A. 1800; P.F. ·8; Phase 3; Volts 6300–6600; Amps per ter. 160; Cycles 50; R.P.M. 150; ~~Rotor Amps~~ ......;
~~H.P.~~ ...... Amps p. cond. 160 ~~Amps p. br. arm.~~ By res. Temp. rise 50° C. Regulation 9% PF=1 Overload 25%

Customer ......; Order No. ......; Quot. No. ......; Perf. Spec. ......; Fly-wheel effect ......

Frame 416/33 Circum. 1212; Gap Area 40,000; poss. $A_g B$ 3·8×10⁸; poss. $I_a Z_a$ 300000; $I_a Z_a$; $\frac{D^2 L \times RPM}{K.V.A.}$ = 4·1×10⁵ cm³
Air ......; $A_g B$ 3·66×10⁸; $I_a Z_a$ 288000; Circum. 237;

K_e ·4; 6600 Volts = ·4 × 2·5 × 1800 × 3·66; Arm. A.T. p. pole 3400 ...... Max. Fld. A.T. 11,000 per pole

Armature. ~~Rev.~~ Stat.			
Core.	Dia. Outs.	cm. 416	
	Dia. Ins.	386	
	Gross Length	33	
	Air Vents 5	0·63	
	Opening Min. ___ Mean		
	~~Air~~ Velocity of rotor	30 m. p. sec.	
	Net Length 29·8 × ·89	26·5	
	Depth b. Slots	10·1	
	Section 268 Vol.	342,000	
	Flux Density	11,200	
	Loss ·06 p. cu. cm. Total	20,500	
	Buried Cu. 7800 Total	35,200	43,000
	Gap Area 34,000; Wts	15,100	
	Vent Area 180,000; Wts	16,000	
	Outs. Area 82,000; Wts	12,300	43,400
Teeth.	No of Segs 20 Mn. Circ.	1220	
	No of Slots 360 × 1·3 =	468	
	K_s	752	
	Section Teeth	19,900	
	Volume Teeth	98,000	
	Flux Density	18,400	
	Loss ___ p. cu ___ Total	14,700	
	Weight of Iron	3500 kg.	
Conductors.	Star ~~or Mesh~~ Throw	3–10; 2–11; 1–12.	
	Cond. p. Slot	5	
	Total Conds	1800	
	Size of Cond. ·65 × ·65	·41	
	Amp. p. sq. cm.	390	
	Length in Slots ·33		
	Length outside ·62 Sum	·95 m.	
	Total Length	1710 m.	
	Wt. of 1,000 360 Total	610 kg.	
	Res. p. 1,000 ·415 Total	·72	
	Watts p. metre	65	
	Surface p. metre	1070	
	Watts p. Sq. cm.	·061	
	·0012 × 18° C 0·32	·068	



Field ~~Stat or~~ Rotor.		
Dia. ~~Bore~~	384·8	
½ Total Air Gap	0·51	
Gap Co-eff. K_g	1·1	
Pole Pitch 30·2 Pole Arc	19·5	
K_f	·65	
Flux per Pole 5·96×10⁶	6·86×10⁶	
Leakage n.l. 1·3 f.l. 2·3	8·26×10⁶	
Area 450 Flux density	18,400	
Unbalanced Pull	5400 kg.	
No. of Seg. ___ Mn. Circ.		
No. of Slots ___ × =		
Vents		
K_s ___ Section		
Weight of Iron Poles 3400 kg. 3160 kg. Rings.		

	Shunt.	Series.	Comm.
A.T. p Pole n. Load	5150		
A.T. p. Pole f. Load	9600		
Surface	150,000		
Surface p. Watt	7·35		
I². R.	20,800		
I. R.	104		
Amps.	200		
No. of Turns	48		
Mean l. Turn	110		
Total Length	2100		
Resistance	0·45 cold	0·52 hot	
Res. per 1,000	0·214		
Size of Cond.	·25×3·2	·8 sq. cm	
Conds. per Slot			
Total Length			
Wt. per 1,000 m	720		
Total Wt.	1440		
Watts per Sq. cm.	·138		
Star or Mesh			
Paths in parallel			

Magnetization Curve.	Section.	Length	5700 Volts. B.	A.T. p. cm.	A.T.	6600 Volts. B.	A.T. p. cm.	A.T.	6900 Volts. B.	A.T. p. cm.	A.T.
Core	268	21	9670	4	84	11,200	5	110	11,700	7	147
Stator Teeth	19,900	4·8	15,900	30	144	18,400	98	470	19,200	160	770
Rotor Teeth											
Gap	40,000	·51	7900		3580	9160		4150	9550		4320
Pole Body F.L.	450	9	16,000	40	360	18,400	170	1520	19,200	220	2000
Yoke					4168			6250			7237
Pole Body N.L.					192			420			680
					4000			5130			5917

Commutator.	
Dia. ___ Speed	
Bars	
Volts p. Bar	
Brs. p. Arm	
Size of Brs.	
Amps p. sq.	
Brush Loss	
Watts p. Sq.	

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.	15	15	15	15	15
Iron Loss	35·2	35·2	35·2	35·2	35·2
Field Loss	25	22	19·4	16	12
Arm. &c. I²R	35	22	12·2	5·2	1·3
Brush Loss					
	110·2	94·2	81·8	71·4	63·5
Output	1800	1440	1080	720	360
Input	1910	1534	1162	790	423
Efficiency %	94·2	93·9	93	90	85

Mag. Cur. Loss Cur.
Perm. Stat. Slot
" Rot. Slot × =
" Zig-zag
2 × × =
1·77 × =
End × × =
Amps; Tot.
τ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

This enables the length to be slightly reduced. The general method of working out the machine is as before. In this case the pole body has been made 16·5 cms.

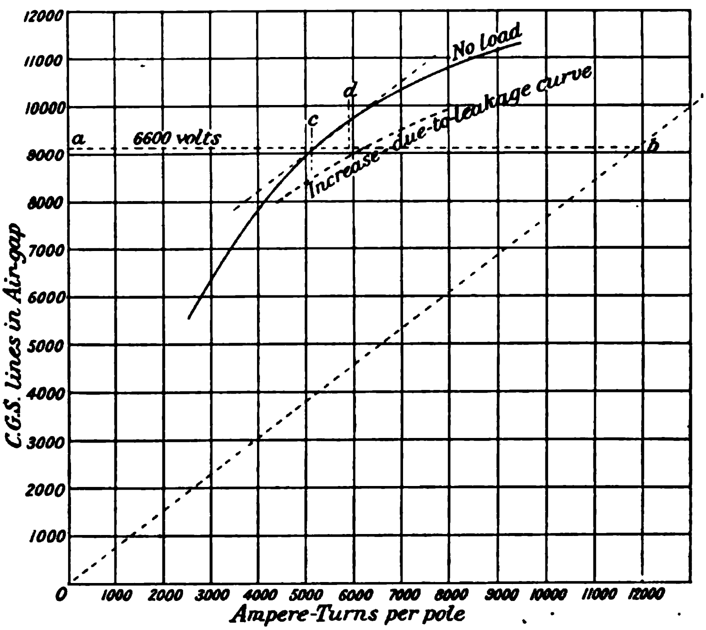


FIG. 347.—Magnetization curve of 1800 K.V.A. generator, showing method of finding effect of saturation on magnetic pull.

wide and of 28 cms. axial length, instead of 18 cms. wide and 27 cms. axial length. The effect is to give a little more cooling space between the coils, and a little more saturation in the poles.

The no-load magnetization curve and the increase-due-to-leakage curve of this machine are given in Fig. 347. The method of working out the effect of the saturation on the unbalanced magnetic pull is given on page 60.

CHAPTER XIV.

ALTERNATING-CURRENT GENERATORS (*continued*).

WATER-TURBINE TYPE.

SPECIFICATION No. 4.

2500 K.V.A. THREE-PHASE GENERATOR TO BE DRIVEN BY A WATER TURBINE.

31. The Contractor shall supply and erect at the power-house of the Purchaser, situated at , a generator having the characteristics specified below. **Extent of Work.**

32. **Characteristics of Generator.**

Normal output	2500 K.W.* at unity power factor. 2500 K.V.A. at 0·8 power factor.
Power factor of load	Between unity and 0·8.
Number of phases	3.
Normal voltage	6900.
Voltage variation	6800 to 7000.
Amperes per phase	210.
Speed	600 revs. per minute.
Frequency	50 cycles per second.
Regulation	12 per cent. rise with non-inductive load thrown off, the speed and excitation being constant. 18 per cent. rise with 0·8 power factor load thrown off, the speed and excitation being constant.
Over load	263 amperes at 6900 volts power factor between 0·9 and unity.
Exciting voltage	90 volts.

* Where the power factor of the load will probably be near unity, it is best to call for the full K.V.A. at unity power factor and a reduced K.W. at lower power factors.

Temperature rise after 6 hours full load	45° C. by thermometer. 55° C. by resistance.
Temperature rise after 2 hours over load	55° C. by thermometer. 65° C. by resistance.

Horizontal Shaft. 33. The generator must run on horizontal bearings, and be designed to be directly coupled to a water turbine with horizontal shaft.

Coupling. 34. Both halves of the coupling will be supplied by the makers of the turbine, who shall be responsible for the proper working of the coupling.

Shaft. 35. The shaft and other parts of the generator shall be strong enough to withstand the shocks which may come upon it if the generator is short circuited at full voltage.

Foundations. 36. The foundations for the generator will be supplied by the Purchaser, but the Contractor shall supply the holding-down bolts and plates, and be responsible for the erection on the foundations and the grouting in of the bedplate.

Particulars for Foundations and Coupling. 37. Within ten weeks of the receipt of the order, the Contractor shall supply sufficient particulars of the bedplate to enable the foundations to be laid out, and at a convenient time shall supply the foundation bolts and plates and a template for setting out the same. He shall also supply within ten weeks of receipt of the order sufficient particulars of the shaft to enable the coupling to be manufactured.

Rotor designed for 80 % over Speed. 38. The revolving part of the generator shall be designed to run with safety at a speed of 1080 R.P.M., so that in the event of the water turbine running away no serious accident shall happen. The generator shall be run at 1080 R.P.M. for ten minutes at the Contractor's works before being despatched.

Running Conditions. 39. The generator is to form one of a number of similar machines, each coupled to its own turbine and electrically connected in parallel on the same bus-bars. The load will consist of a general electric supply to various towns and villages lying within five miles of the power-house and fed by overhead lines. The generator shall be suitable in every way for this work.

Plan of Power House. 40. A plan of the power-house accompanies this specification, showing the positions of the proposed water turbines

and generators and the space available for the same. The general method of carrying the weight of the machinery is indicated.

41. The Contractor shall supply with the generator a bed-plate or sole plates suitable for the proposed foundations. There shall be two self-aligning bearings fitted with automatic oiling arrangements and means of adjustment. Bedplate.

42. The cables from the terminals to the switchboard will be provided by the Purchaser. Cables.

43. The generator shall be star-connected, and the centre point of the star shall be brought to a terminal which shall be clearly marked " star point." Star Point.

44. The ends of the three-phase winding may be brought out from the armature by means of cables provided with suitable sleeves for connecting to the Purchaser's cables. Such terminal cables shall be insulated with waterproof flexible insulation material of high quality, which shall not be rubber or any material liable to be softened by heat. Terminals.

Here may follow the following clauses, or such of them as are suitable under the circumstances of the case: Clauses Nos. 8, 9, 10, 12, 13, 14, 15, 16, 17, 18, 19, 20, 23, 26, 27, 60, 61, 69, 73, 74.

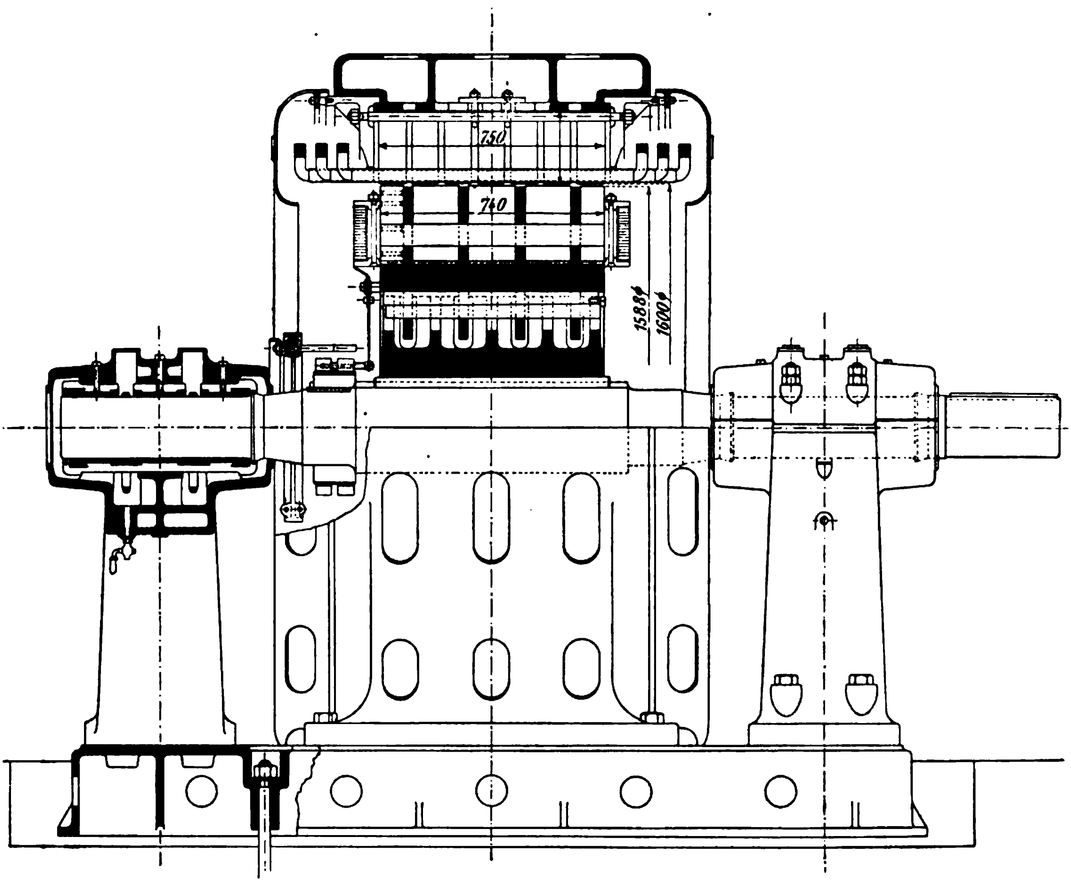
DESIGN OF A 2500 K.V.A. WATER-DRIVEN, THREE-PHASE GENERATOR TO MEET SPECIFICATION No. 4.

The main difficulty in the design of generators of high output and high speed, such as are required for connecting to water turbines, lies in providing a sufficient factor of safety in the mechanical design. It is usual to provide a fair factor of safety at a speed 80 per cent. higher than the running speed, and as this is usually already high, a special construction of the field-magnet is required to resist the great centrifugal forces. The centrifugal forces are not in general as high as in some turbo-generators; but as the number of poles on water-driven generators is usually great enough to make the use of salient poles economical, one commonly finds on these machines a type of construction peculiar to them. The pole pieces are very often made separate from the field spider, so as to admit of overhanging pole spurs to support the field coils. Where the pole pieces are held on by bolts it is usually necessary to provide a very large number of bolts for each pole, so that a large percentage of the pole area consists of the cross-section of bolts. This is rather expensive. A cheaper construction is to provide a very large dovetail or two dovetails at the root of each pole. Another construction is that shown in Figs. 348 and 349, in which portions of the polar extensions are interleaved with portions of the spider, and provided with cotter pins or bolts running axially. This

construction makes good provision for resisting the centrifugal forces, and allows the field coils to be put under considerable pressure.

The design which we have taken to meet Specification No. 4, is one by the Oerlikon Company. It is illustrated in Figs. 348 and 349. The calculation sheet is given on page 364.

It is unnecessary to go through this calculation sheet in detail, as the general method of design will be understood from the description of the 600 K.W. on pages



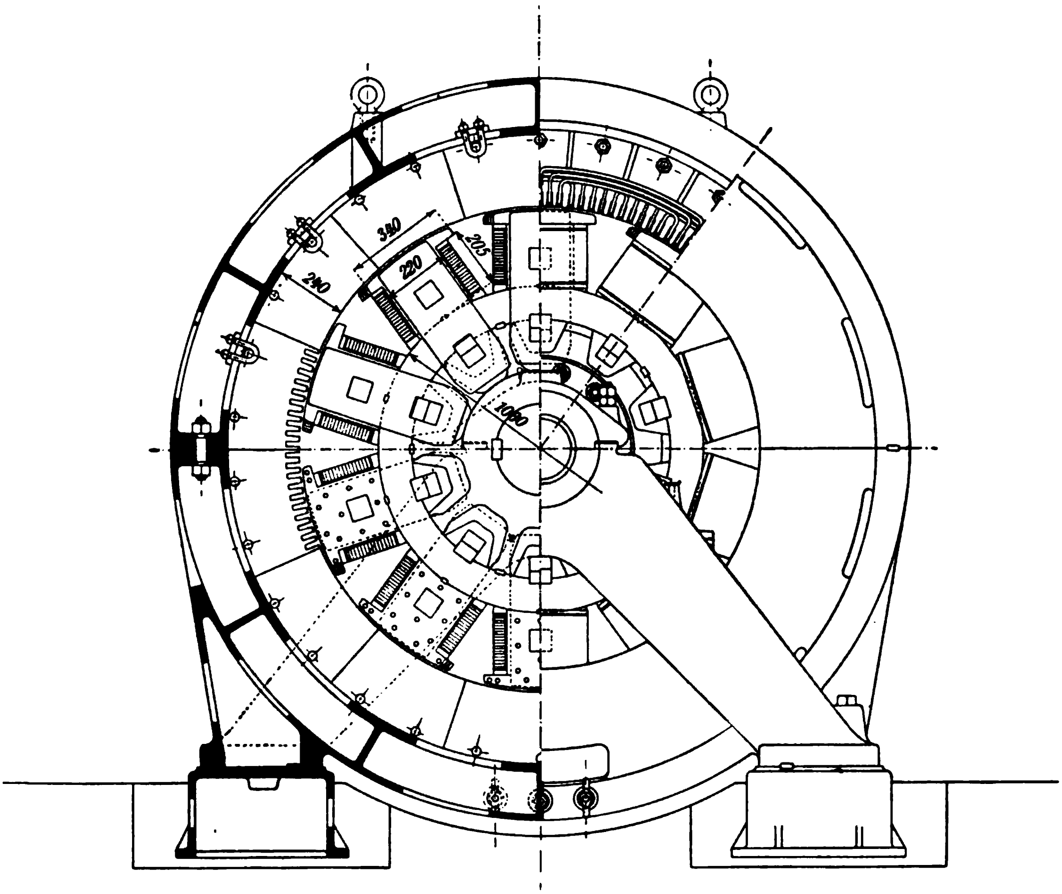
FIGS. 348 and 349.—2500 K.V.A. 3-phase generator, 7000 volts, 50 cycles, designed to be coupled to

316 to 332. The diameter is usually fixed by taking the largest diameter which can be built economically with a sufficient factor of safety. If too small a diameter were chosen, the axial length of these machines of great output would be too great and the cooling conditions would be bad. In this case the diameter is 160 cms., and with a D^2l constant of $4{\cdot}6 \times 10^5$ the axial length comes out at 75 cms. The peripheral speed is 50 metres per second, or just about 10,000 feet per minute —a suitable speed with this construction of rotor.

The tips of the pole are made to well overlap the coils, so as to give good mechani-

cal support, and thus the ratio of pole arc to pole pitch is rather high, namely 0·71. As there is very little bevel on the pole, the electromotive coefficient K_e comes out as high as 0·43. In other respects the working out of the machine follows very closely the method given in the previous examples.

As this machine has salient poles, it is of interest to work out the ampere-turns required at full load 0·8 power factor by the method described on page 294, and to compare the result with that obtained by the method described on



a water turbine running at 600 revs. per minute. See Specification No. 4 and Calculation Sheet No. 4.

page 280, and with the figures obtained by the use of the curves given in Fig. 312.

The first step is to plot the magnetization curve. The figures for 6200, 7200 and 8600 volts are given in the calculation sheet, and the magnetization curve E is shown plotted in Fig. 349a. Fig. 314 will serve for our graphic construction, but is not to scale, as we will apply new values to the various vectors.

The armature ampere-turns per pole, I_{za}, are 5500. The terminal voltage E_t is 6900. $I_a x_a$ is 350, $I_a r_a$ is 70, making E_g 7200. We find the approximate position

Date *16 July* 19*13* Type *W.T.* *A.C.* GEN. ~~SYN MOTOR ROTARY~~ *10* Poles ......Elec. Spec. *4*

K.V.A. *2500*; P.F. *·8*; Phase *3*; Volts *6900*; Amps per ter. *210*; Cycles *50*; R.P.M. *600*; Rotor Amps ......;

H.P. ......Amps p. cond. *210* . Amps p. br. arm ...... Temp rise *45°C* ......Regulation *18%* *P.F.=·8* Overload *25% 2 hrs*

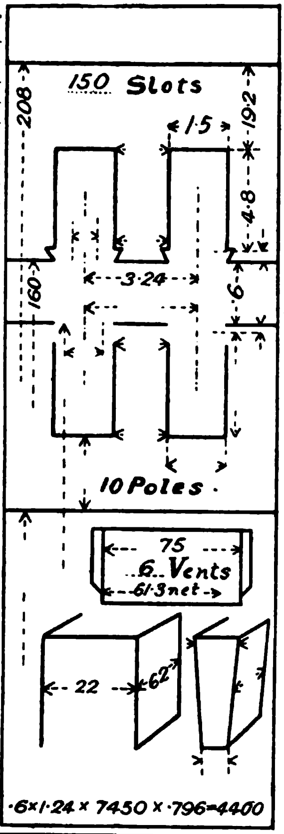
Customer ......; Order No......; Quot. No......; Perf. Spec. *No 3* ......; Fly-wheel effect ......

Frame *208/75* Air ...... Circum. *502*; Gap Area *37,500*; poss. $A_g B$ ......; $A_g B$ *2·79 × 10⁸*; poss. $I_a Z_a$ ......; $I_a Z_a$ *125,000*; $\frac{I_a Z_a}{\text{Circum.}}$ *250*, $\frac{D^2 L \times RPM}{K.V.A.}$ *4·6 × 10⁵*;

K_e *·43* ; *7200* Volts = *·43* × *10* × *600* × *2·79* ; Arm. A.T. p. pole *5500* ...... Max. Fld. A.T. *11000*

Armature. ~~Rev.~~ Stat.

Core.	Dia. Outs.	*208*	
	Dia. Ins.	*160*	
	Gross Length	*75*	
	Air Vents *6 × 1*	*6*	
	Opening Min. ____ Mean		
	Air Velocity	*50m. per sec.*	
	Net Length *69* × ·89	*61·3*	
	Depth b. Slots	*19·2*	
	Section *1170* Vol.	*69000*	
	Flux Density	*8500*	
	Loss *·038* p. cu. *cm.* Total	*26,000*	
	Buried Cu. *9600* Total	*35,700*	*45,300*
	Gap Area *37,500*; Wts	*22,500*	
	Vent Area *170,000*; Wts	*24,000*	
	Outs. Area *75,000*; Wts	*11,000*	*57,500*
Teeth.	No of Segs *10* Mn. Circ.	*511*	
	No of Slots *150* × *1·5* =	*225*	
	K_s	*286*	
	Section Teeth	*17,500*	
	Volume Teeth	*8,400*	
	Flux Density	*16,000*	
	Loss *·115* p. cu *cm* Total	*9,700*	
	Weight of Iron		
Conductors.	Star ~~or Mesh~~ ____ Throw		
	Cond. p Slot	*4*	
	Total Conds	*600*	
	Size of Cond. *·75 × ·75*	*0·56 sq. cm.*	
	Amp. p. sq.	*375*	
	Length in Slots *75*		
	Length outside *101* Sum		
	Total Length *176*	*1050*	
	Wt. of 1,000 *500* Total	*525 kgs.*	
	Res. p. 1,000 *·304* Total	*·322*	*0·38 ht*
	Watts p. *m* *71 × 1·2*	*85*	
	Surface p. *m*	*1000*	
	Watts p. Sq.	*·085*	



Field ~~Stat. or~~ Rotor.

Dia. ~~Bore~~	*158·8*	
½ Total Air Gap	*0·6*	
Gap Co-eff. K_c	*1·24*	
Pole Pitch *50* Pole Arc	*34*	*35·7 eff.*
K_f	*·71*	
Flux per Pole	*19·8 ×*	*10⁶*
Leakage n.l. ____ f.l. *2·8*	*22·6 ×*	*10⁶*
Area *1295* Flux density	*17400*	
Unbalanced Pull		
No. of Seg. ____ Mn. Circ.		
No. of Slots ____ × =		
Vents		
K_s ____ Section		
Weight of Iron		

	Shunt.	Series.	Comm.
A.T. p Pole n. Load	*6350*		
A.T. p. Pole f. Load	*11000*		
Surface	*98000*		
Surface p. Watt	*6·5 poss.*	*11·4 actual*	
I². R.	*8550*		
I. R.	*58·5*		
Amps.	*146*		
No. of Turns	*75·5*		
Mean l. Turn	*2·05 m.*		
Total Length	*1550*		
Resistance	*·35 cold,*	*·4 hot*	
Res. per 1,000	*·233*		
Size of Cond.	*·19 × 4*	*·73 sq. cm.*	
Conds. per Slot			
Total Length			
Wt. per 1,000 *m*	*650*	*Kgs*	
Total Wt.	*1000*		
Watts per Sq. *c.m.*	*·088*		
Star or Mesh			
Paths in parallel			

Magnetization Curve.	Section.	Length	*6200* Volts. B.	A.T. p. cm	A.T.	*7200* Volts. B.	A.T. p. cm	A.T.	*8600* Volts. B.	A.T. p. cm	A.T.
Core	*1170*	*20*			*40*	*8500*	*3*	*60*			*90*
Stator Teeth	*17,500*	*4·8*	*13,800*	*13*	*62*	*16,000*	*35*	*168*	*19,200*	*189*	*900*
Rotor Teeth											
Gap	*37,500*	*·6*			*3780*	*7450*		*4400*			*5260*
Pole Body	*1295*	*20·5*	*15,000*	*40*	*820*	*17,400*	*75*	*1550*	*20,500*	*486*	*9900*
Yoke	*690*	*13*	*14,000*	*35*	*450*	*16,200*	*50*	*650*	*19,300*	*175*	*2270*
					5152			*6728*			*18420*

Commutator.

Dia. ____ Speed	
Bars	
Volts p. Bar	
Brs. p. Arm	
Size of Brs.	
Amps p. sq.	
Brush Loss	
Watts p. Sq.	

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.		*25*			
Iron Loss		*36*			
Field Loss		*10*			
Arm. &c. I²R		*26*			
Brush Loss					
		97			
Output		*2000*			
Input		*2091*			
Efficiency %		*95·6*			

Mag. Cur. Loss Cur.
Perm. Stat. Slot *1·5*
„ Rot. Slot × =
„ Zig-zag
2 × *75* × *1·5* = *225* =
1·77 × *210* × *4* × *225* = *3·3*
End *2·5* × *70* × *4200* = *7·3*
Amps; Tot. *10·6 × 10⁶*
τ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

Leakage $\frac{1·06}{19·2}$ = *5·5%* *380 volts*

of the centre line of the pole by the construction given in Fig. 314, and by trial and error find I_{zc} equal to 3100 ampere-turns, and I_{zd} equal to 4500 ampere-turns.

We must now refer to Fig. 313, and we find that for a ratio of pole arc to pole pitch of 0·71 the coefficient K_ϕ is 0·4. And

$$0{\cdot}4 \times 3100 = 1240 \text{ effective cross-mag. A.T.}$$

Dividing 1240 by 75·5 turns per pole, we get 16·4 amperes exciting current, which, from Fig. 349*a*, we see would give us 1950 volts generated in the armature. Thus E_c in Fig. 314 = 1950, and gives us the true position of the centre line of the pole. We now find that E is about 6900 volts, which requires 6350 A.T. per pole on the field. If we add the demagnetizing 4500 ampere-turns of the armature to the 6350, we get 10,850 ampere-turns per pole required at full load. Now compare this result

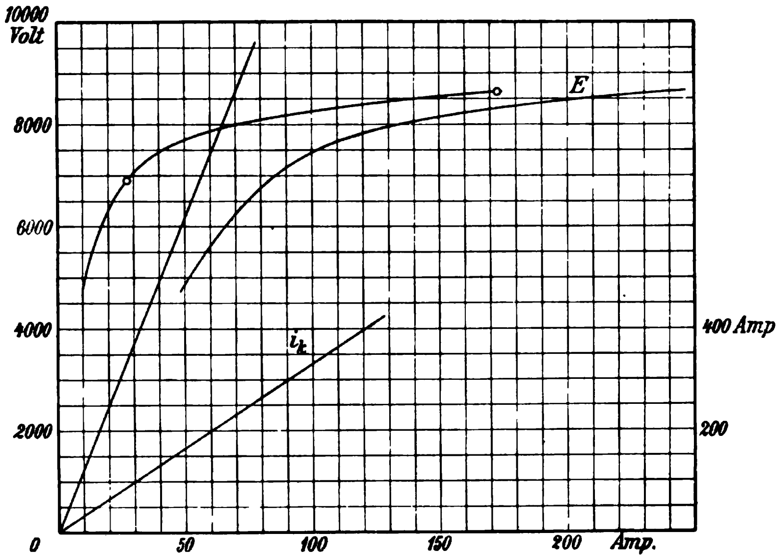


FIG. 349*a*.—Magnetization curve, E, and short-circuit characteristic, i_K, of a 2500 K.V.A. 3-phase generator.

with that obtained by the construction given in Fig. 305. To generate 7200 volts we require 6728 effective ampere-turns per pole. Set off the vector I_{zr} to represent 6728, and the vector I_{za} to represent 5500, as in Fig. 305. The sum is 11,000 ampere-turns, so that the difference between the results obtained by the two methods is not of great importance. Now find the ratio of short-circuit ampere-turns to no-load field ampere-turns. The demagnetizing ampere-turns are 5500, and those required to overcome the armature reaction are 230, making 5730.

$$\text{The ratio } \frac{5730}{6350} = 0{\cdot}9.$$

Taking now the abscissa 0·9 in Fig. 312, we find that for 0·8 power-factor load we require 70 per cent. increase in the field current. This gives us :

$$6350 \times 1{\cdot}7 = 10{,}800 \text{ A.T. per pole,}$$

which again is not very far from the mark. We see therefore that any of these methods gives a result sufficiently near the truth.

CHAPTER XV.

ALTERNATING-CURRENT TURBO-GENERATORS.

ALTERNATING-CURRENT turbo-generators differ from slow-speed machines both in the design of the stationary armature and in the construction of the revolving field-magnets. The special modes of clamping the windings on turbo-armatures have been considered on pages 119 to 131; and the manner of ventilation has been considered on pages 205 to 217. We will consider here a few points relating to the revolving field-magnet, before passing on to consider the design of some machines in detail.

FIG. 350.—Field magnet of 6000 K.W. turbo-generator, having salient poles; speed 1800 R.P.M. (Westinghouse Co.).

The high speed of turbine-driven generators gives rise to very great centrifugal forces, which necessitate very strong construction in order to secure the parts of the rotor. The number of poles on a high-speed machine of normal frequency is necessarily low, and this leads to the use of wide and bulky field-coils, the supporting of which makes the problem especially difficult. On the early turbo-generators, salient poles, each with a single field-coil, were employed; but it soon became evident that the field-coil should be split up into small sections, each of which could be independently supported in a more mechanical manner than was possible when large aggregations of insulation and copper were employed. Fig. 350 illustrates a successful form of salient pole machine built by the Westinghouse Company of America. It consists of two steel castings, extensions of which are forged down to form the shaft. The main bodies of the castings are spigotted as shown, and twelve bolts near the periphery hold the castings together. Four slots are then

planed around each pole to receive the field-windings, which are wound directly in the slots and insulated with mica. These coils are retained by means of brass wedges closing the mouths of the slots, so that the whole field-magnet is enclosed in metal. The method of ventilation is clearly seen in the figure. This construction is exceedingly good from the mechanical point of view. It will be seen, however, that the cross-section of the magnetic circuit is somewhat more restricted than it would be in a cylindrical field-magnet (see Fig. 351).

From a theoretical point of view, leaving out of account all the difficulties of supporting the parts mechanically, the ideal arrangement of copper and iron on a four-pole field-magnet would be one which gives the greatest possible cross-section to the iron paths, while the whole of the space between the poles is occupied

FIG. 351.—Cylindrical field-magnet built up of punchings.

by the copper of the field winding. It will be seen that a cylindrical field-magnet with copper placed in slots near the periphery, more nearly approaches the ideal arrangement than the salient-pole rotor.

There are several advantages to be obtained by placing the exciting winding in radial slots. (1) Each section of the winding is well supported by the teeth. (2) The cooling is good, because no part of the copper is very far removed from the iron teeth, and the total coil surface is great compared with its volume. (3) The effective width of the pole is not merely the width of the smallest coil, but extends across the pole pitch. Thus, while the copper is divided into sections and can be worked at a high-current density, the space between the coils is not wasted, but is used for the magnetic circuit. (4) It is desirable in many cases that the iron of the magnetic circuit near the periphery of the field-magnet should be saturated. The cutting away of the iron to make room for the copper is in this case a gain rather than a loss. This will be seen more clearly when we consider the shape of the field form of a cylindrical field-magnet. (5) The magnetic field-form can be made approximately sinusoidal, and results in a wave-form of electromotive force,* very near to the true sine wave, both at no load and at full load.

* See "The Non-salient Pole Turbo-Alternator," S. P. Smith, *Journ. I.E.E.*, vol. 47, p. 562.

The saturation which occurs at the root of a salient pole is not as effective in improving the regulating quality of a field-magnet as saturation occurring near the face of the pole. Where saturation occurs at the root of a pole, it will be found that the ampere-turns are very much increased on loads of low-power factor; because not only have ampere-turns to be added to overcome the normal saturation of the pole, but extra ampere-turns must be added to overcome the excessive saturation created by the leakage flux. Fig. 352 shows the form of the no-load magnetization curve of a salient-pole machine, which had 20 per cent. of its field ampere-turns expended on the iron at no load, 9000 volts. When full load ($\cos\phi = 0{\cdot}8$) was thrown on the machine, the ampere-turns had to be increased to more than double their value at no load, because the full-load magnetization

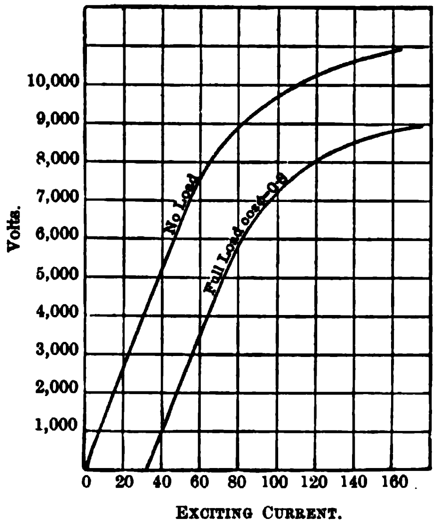


FIG. 352.—No-load and full-load characteristics of salient-pole generator.

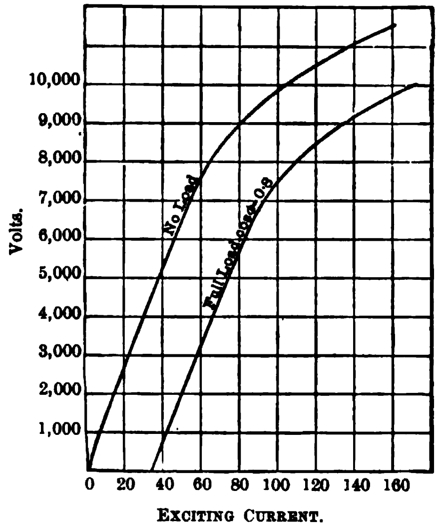


FIG. 353.—No-load and full-load characteristics of cylindrical field-magnet.

characteristic curved over so as to become almost horizontal at full voltage. Fig. 353 shows the general character of the magnetization curves on no load and full load of a generator with a cylindrical field-magnet. The saturation occurring on the surface of the pole has been adjusted so as to give 20 per cent. of the ampere-turns expended on the iron at no load, 9000 volts. With this construction it would be possible to obtain 9000 volts full load ($\cos\phi = 0{\cdot}8$) with an increase in the ampere-turns of not more than 70 per cent.

The body of the rotor. There are three general methods of constructing the body of the rotating field-magnet. (1) It may consist of punchings or plates built upon a central shaft, as shown in Figs. 351, 220. (2) It may consist mainly of the shaft itself, whose diameter is sufficiently great to allow dovetail slots to be cut in it, as shown in Figs. 354, 355, into which slots iron teeth are fitted. (3) The whole rotor may be cut out of a solid cylinder of steel, as shown in Fig. 362.

Rotor built of punchings. The advantage of building up the rotor of steel punchings or plates is that it enables the manufacturer to use rolled materials

of great strength; and the punching of slots and ventilating holes of the required shape is a comparatively cheap process. The disadvantage is, that the diameter of the shaft cannot in general be made great enough to give to the whole rotor a

FIG. 354.—Iron parts of two-pole turbo field-magnet by A.E.G.

stiffness which will make the critical speed higher than the running speed. Most turbo field-magnets built up of punchings or plates have a critical speed lower than the running speed. Very many successful machines have been made in this way, and no difficulties are experienced in the balancing or running where the proper precautions have been taken. Rotors of this type are illustrated in Figs. 220, 351 and 367.

Rotor with dovetail teeth. The second method of building up the rotor is very well shown in the two-pole field-magnet built by the Allgemeine Elektricitäts

FIG. 355.—Cross-section of two-pole turbo field-magnet by A.E.G.

Gesellschaft, illustrated in Figs. 354 to 357, designed to run at 3000 revs. per minute. Here we have a shaft of diameter sufficiently great to give the whole rotor a critical speed higher than the running speed. In this shaft are milled dove-tail grooves, as shown in Figs. 354 and 355, and into these grooves are driven

teeth, so as to secure the field-coils against the great centrifugal forces. With this type of construction, it is possible to build up the field-coils and teeth as a complete whole, and to push them longitudinally into place on the shaft; or, with a slight

FIG. 356.—Two-pole turbo field-magnet by A.E.G., showing the exciting coils in position.

modification of the construction, the field-coils can be put on one by one, beginning with the largest coil, and the teeth inserted afterwards. One great advantage of this construction is that it enables each field-coil to be completely formed and

FIG. 357.—Two-pole turbo field-magnet for 3000 K.W. running at 3000 R.P.M.

insulated before it is put on to the rotor. Fig. 356 shows the field coils in position before banding. It will be seen that the dovetails at the end of the rotor form ventilating ducts which supply air to the end windings and the ventilating holes punched in the body of the teeth (see Fig. 354). The ends of the windings are secured by steel wire wound over a metal casing, the whole rotor being finished in the manner shown in Fig. 357. Drawings of stator and rotor of a completed machine are given in Figs. 376 and 377 (page 406).

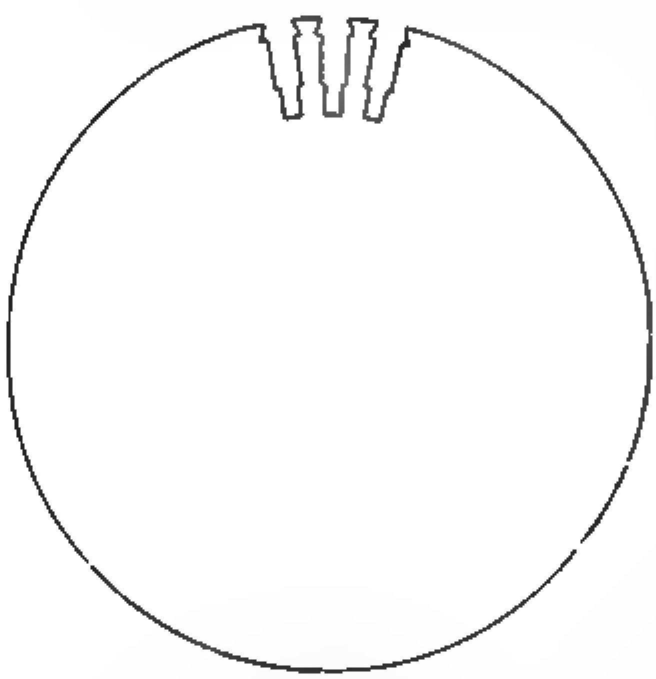


FIG. 358.—Showing shape of slots and ventilating ducts in turbo field-magnets cut out of the solid.

Solid rotor. Many manufacturers prefer to construct the rotor of one solid steel forging, and to plane out the slots for the reception of the winding. In this case it is convenient to provide ventilating ducts immediately below the slots. The form of the slots and ducts may be as indicated in Fig. 358. This construction gives great lateral stiffness, and enables rotors of great length to be built, which have a critical speed higher than their running speed.

One of the main difficulties in the design of cylindrical field-magnets lies in the supporting of the field coils where they project at the ends of the rotor.

Rotor windings: Two-pole windings. Where the slots are radial and the teeth are immovable, the only way of inserting the field-coils is by putting them in turn by turn. The shape of field-coils used on solid rotors with radial teeth is shown in Fig. 359. In this case there are eleven coils per pole, and it will be seen that while the cooling surface of the parts of the coils lying in the slots is exceedingly great, the cooling conditions of the projecting ends of the coils where they are closely huddled together are not very good. The method of calculating the temperature

FIG. 359.—Field coils of two-pole turbo field-magnet.

rise on a winding of this type is given on page 227. A winding consisting of five double coils is shown in developed plan and sectional elevation in Fig. 360. Complete rotors of this type are shown in Figs. 221, 361 and 378.

Some makers have constructed very successful two-pole field-magnets with barrel end-connectors (see page 115). A rotor of this type is illustrated in Fig. 220. This construction has a great deal to recommend it from the mechanical point of view; but on two-pole machines the end-connectors project from the active iron very much further than with the "coil" type winding illustrated in Figs. 359 and 360.

Another type of two-pole winding which has been successfully developed by the Westinghouse Company of America is shown in Fig. 362. Here the rotor consists of a solid steel forging of cylindrical shape, in which parallel slots have been cut in the sides and end. The winding consists of copper strap wound directly in the slot, insulated with mica, and secured by means of bronze wedges. After

this part is wound, flanges of bronze which carry the shaft are fastened at either end of the field cylinder by massive screws. In the case shown in Fig. 362, the

FIG. 360.—Cross-sections and developed plan of two-pole field winding of 1500 K.W. turbo-generator, running at 3000 R.P.M.; scale [illegible]th full size.

shaft ends in a boss which has teeth machined in it not unlike a large bevelled wheel. The bronze is cast around this boss, filling the dovetails between the teeth,

and making a good rigid connection. The rigidity of this construction is shown by the fact that the critical speed is higher than the running speed, even when the latter is as high as 3600 revs. per minute. Generators running at this speed are built of capacities as high as 5000 K.V.A.

FIG. 361.—Two-pole field-magnet by Siemens Schuckert Co.

Rotor windings: Four-pole windings. When there are four poles, the end-connections of the windings are much shorter than on two-pole windings. There is consequently much less likelihood of overheating. End-connectors of the barrel form illustrated in Fig. 133 are quite suitable, and do not project too far from the iron when the pole pitch is only one-fourth of the circumference. Fig. 363 shows a finished four-pole rotor of 3000 K.W. capacity, the end windings of which are secured by steel bands.

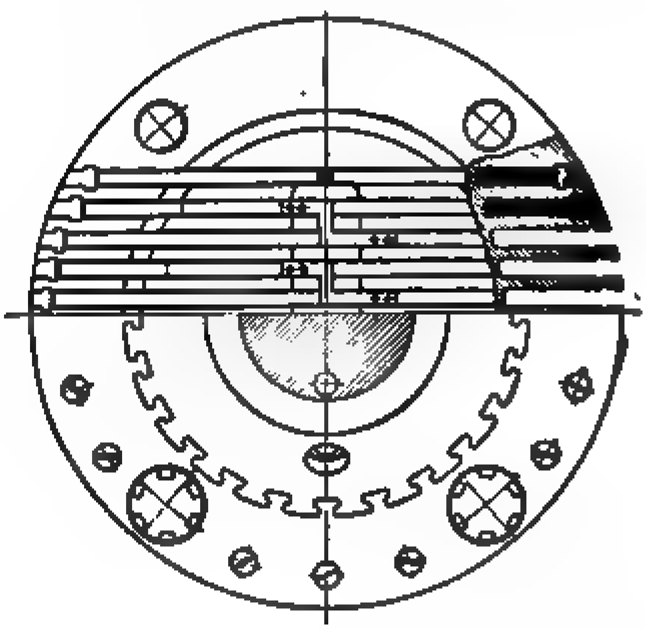


FIG. 362.—Two-pole turbo field-magnet cut out of solid cylinder of steel with the shaft bolted to ends (Westinghouse Company).

A type of winding which is very suitable for four-pole machines is that illustrated in Figs. 364, 365 and 371. The conductors lying in the slots consist of simple bars, suitably insulated, with ends projecting in the manner shown in Fig. 364. These bars are connected in series with one another by end-connectors mounted between two steel cheeks, which grip the end-connectors—in the same way as the bars of a commutator are gripped—by means of V-rings insulated with mica. There are several advantages in this type of construction. The cross-section of the end-connectors can, if necessary, be made greater than the cross-section of

FIG. 363.—Four-pole turbo field-magnet with barrel winding, secured by means of steel banding wire. Rating 3000 K.W. at 1500 R.P.M.

FIG. 364.—Four-pole turbo field-magnet, bar wound, with end-connectors assembled between steel cheeks (British Westinghouse Company).

the bars. As there is plenty of room for the steel cheeks, these can be made very massive, so that a higher factor of safety can be obtained than in those constructions where the amount of steel in the end bell is limited by the space available for it. Moreover, if it is desired to replace the conductors in any one slot, this can be done without interfering with other parts of the winding. Fig. 365 shows the manner in which the end-connectors are mounted during the process of manufacture. Figs. 371, 372 give detail drawings of this type of winding.

FIG. 365.—End-connectors of bar-wound turbo field-magnet, built up and machined ready for clamping between mica V-rings.

The field-form of cylindrical field-magnets. Dr. Stanley P. Smith, in a paper before the Institution of Electrical Engineers,* has very fully investigated the field-form of the cylindrical rotor, and has shown that when the winding space occupies from 0·6 to 0·9 of the pole pitch the effect of all harmonics higher than the fifth can be neglected. As the winding factor (see page 306) for the fifth harmonic is only 0·19, and as the third harmonic is completely neutralized on a star-connected, three-phase machine, the resulting terminal pressure is very close indeed to a true sine wave. The paper gives the values of the harmonics for different winding widths, both with and without saturation, and clearly sets out the analytical

* "The Non-salient Pole Turbo-Alternator and its Characteristics," *Journ. I.E.E.*, vol. 47, p. 562.

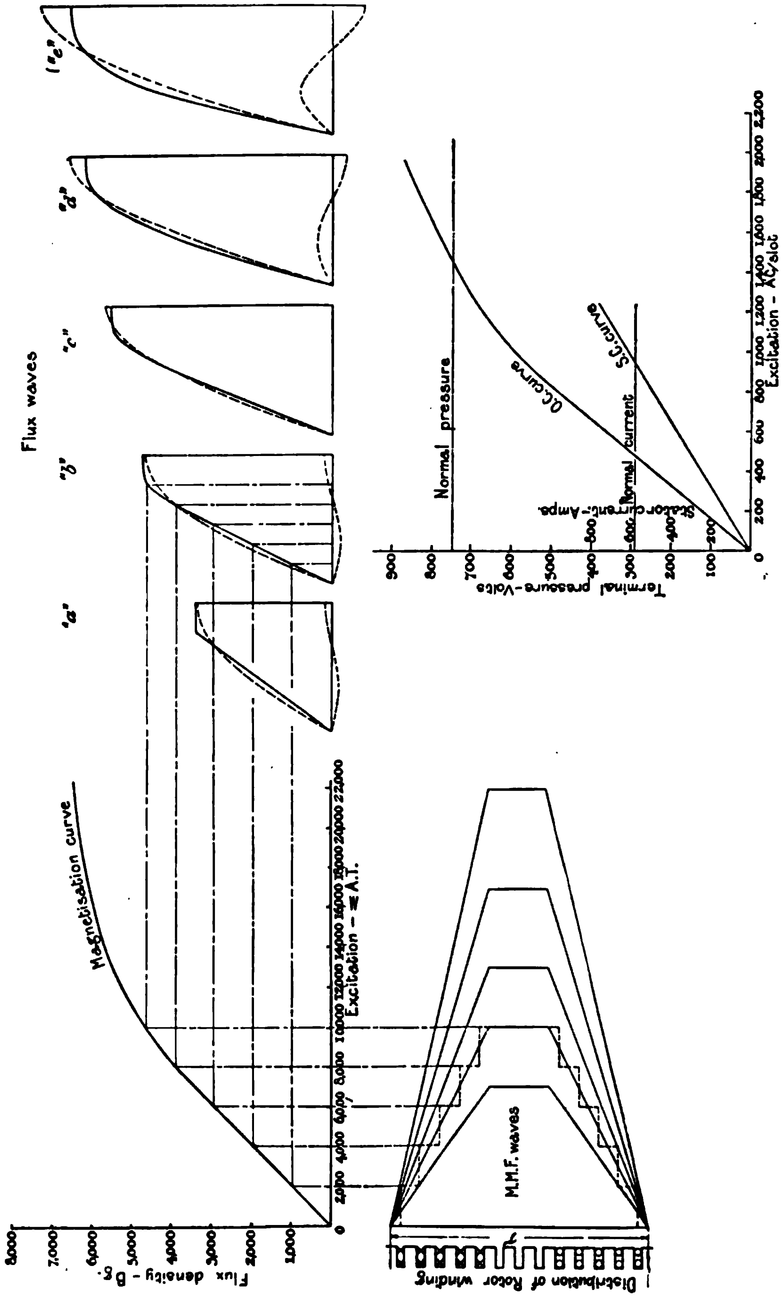


FIG. 366.—Showing the method of plotting the field-form of a cylindrical field-magnet for different amounts of saturation.

method of arriving at the resulting E.M.F.* The effect of armature reaction is also fully dealt with. With any given cylindrical field-magnet the field-form will change somewhat, as the saturation of the iron is increased. At low saturations the curve showing the flux-density in the gap follows very closely the magnetomotive force curve; but as the saturation is increased, the corners of the magnetomotive force curve are rounded off in the manner shown in Fig. 14, page 19. The manner of plotting the field-form for different excitations will be clearly understood from Fig. 366, which is taken from Dr. Smith's paper. The first step is to plot an air-gap-and-tooth-saturation curve, as described on page 78, the abscissa being ampere-turns, and the ordinate the flux-density in the gap. If, then, curves showing the distribution of magnetomotive force along the rotor face are drawn for various excitations in the manner shown in Fig. 366, vertical lines can be run up from these, and where the lines cut the magnetization curve horizontal lines can be projected which give the flux-density at the corresponding points on the rotor periphery; so that the curve of flux-density can be plotted with ease. The figure shows, in dotted lines, the fundamental sine wave, and also the third harmonic. By taking several field-forms in this way, and calculating the voltage generated in the winding, we can plot the open-circuit characteristic of the machine as shown in Fig. 366. An example is worked out in connection with a 15,000 K.V.A. generator on page 395. The field-forms and magnetization curves are given in Figs. 373 and 375.

The specification of A.C. turbo-generators. The main provisions in the specification will be the same as for slow-speed generators. Clauses are sometimes added to ensure sound mechanical construction, and in view of the cost of the plant and of the very great importance of continuity of service, the specification is sometimes made more elaborate than for smaller machines.

The model specification given below contains more clauses than are really necessary. A variety of clauses are given in case the circumstances should call for them; but we must remember that it is always desirable to keep the specification as simple as possible, so that a manufacturer may not be hampered in supplying his standard machinery.

* The reader is referred to pages 305 to 316 on the subject of E.M.F. wave-forms.

SPECIFICATION NO. 5.

15,000 K.V.A. THREE-PHASE TURBO-GENERATOR.

Extent of Work. 51. This specification provides for the supply, delivery on site, erection, testing and setting to work in the power station at   of two Turbo-Alternators (together with the steam turbines, condensers, air-filters and auxiliary plant described in the specifications issued with this one and bearing an even date).

Rating and General Characteristics. 52. Each of the turbo-generators shall have the characteristics set out below :

Normal output	15,000 K.V.A. or 12,000 K.W.
Power factor of load	0·8.
Number of phases	3.
Normal volts	11,000.
Voltage variation	10,000 to 11,500.
Amperes per phase	790.
Frequency	50 cycles per second.
Speed	1500 revs. per minute.
Regulation	22 per cent. rise with full load 0·8 power factor thrown off.
Over load	25 per cent. for 4 hours and 50 per cent. for 15 minutes.
Exciting voltage.	200 volts.
Temperature rise after 6 hours full-load run	45° C. by thermometer, 50° C. by resistance.
Temperature after 4 hours 25 per cent. over load	See clause 62.
Puncture test	23,000 volts alternating applied for 1 minute between armature coils and frame. 1500 volts alternating applied for 1 minute between field coils and frame.

Plan of Site. 53. Plan No. 1 attached to this specification shows the proposed general lay-out of the power station and the position of the new turbo-generators.

54. The proposed general arrangement of the power plant is shown in plan in the accompanying Figure 1, and in elevation in Figure 2. **General Arrangement.**

55. The Power station is connected to the Railway by means of a railway siding, and a crane capable of lifting 40 tons will lift weights directly from railway waggons to the central floor of the station, **Accessibility.**

or,

56. The power station has a wharf on the banks of the river . A crane capable of lifting 20 tons will lift weights from barges to the floor of the station. The contractor must make provision for the lifting and handling of weights greater than 20 tons,

or,

57. The power station is half-mile from the nearest railway siding. The contractor must make provision for the carriage of all parts of the machinery to the site in question, and for this purpose he is invited to inspect the site and its approaches,

or,

58. The approach to the power station is along an alleyway, one point of which is not more than 11 feet wide. The contractor must arrange the parts of the machinery so that they can be brought on site through the existing approaches, or if any cutting away of brickwork should be necessary, this must be made good at the contractor's expense,

or,

59. The turbo-alternator will have to be transported from the entrance of the station over existing machinery, to the place where it is to be erected. On account of the small headroom, it may be impossible to do this while the existing machinery is running; in that case the bringing in the parts of the new machinery will have to be done between the hours of 2 a.m. and 5.30 a.m., and the contractor must make allowance in his tender for any additional expense which this will cause.

60. There is an overhead travelling crane in the power station capable of lifting 30 tons, which may be used by the contractor at his own risk, when the same is not required by the purchaser or his agents. The contractor must make **Use of Crane.**

provision for the lifting of any weights that are beyond the capacity of the crane.

General Purposes of Plant.

61. The present power station supplies 3-phase power at a pressure of 11,000 volts to the town of , where it is utilised for the driving of cotton-mills and other factories, for traction purposes and for general lighting and domestic use. The turbo-alternators covered by this specification are intended to supplement the plant at present installed, and must be suitable in every way for the purposes aforesaid.

Temperature rise on over load.

62. After a four hours' run with a load of 1000 amperes per phase at 11,500 volts, P.F. 0·8, the temperature rise as ascertained by increase of resistance shall not be such as to make the maximum temperature in any part exceed the value specified by the International Electrotechnical Commission as a permissible temperature, having regard to the nature of the insulation employed.

Wave-Form.

63. The wave-form of the E.M.F. at all loads shall be approximately a sine wave, and at no-load there shall not be any harmonic having an amplitude greater than 1·5 per cent. of the fundamental.

Type.

64. The turbo-generators shall be of the horizontal type with revolving field magnets. The contractor shall state the way in which he proposes to withdraw the field magnet for inspection or repair.

Balance.

65. The revolving parts * shall be balanced with extreme accuracy, so that when running only the smallest possible amount of vibration is communicated to the bearings. Means shall be provided whereby the balancing weights can be easily adjusted.

Factor of Safety.

66. At the normal speed of 1500 revs. per minute, the rotors shall have a calculated factor of safety in every part of not less than five. The revolving parts shall, before leaving the contractor's works, be run at a speed of 1700 revs. per minute, without showing any signs of movement of the component parts relatively to one another.

Bearings.

67. Bearings shall be of the self-aligning type, and shall be so arranged that the bottom half of the bearing may be

* In cases where the purchaser wishes to insist upon having the critical speed higher than the running speed he may add the following clause:

Critical Speed.

The revolving parts of the machines shall be so constructed that the critical speed is not less than 1800 revs. per minute. The purchaser shall be entitled to call for the calculations as to the critical speed, so that he or his agents may check the same.

removed without raising the shaft more than 0·1 inch. Liners shall be provided to facilitate the alignment of the bearings. All bearings shall be interchangeable. Bearings shall not be water-cooled. They shall be lubricated and cooled by a supply of oil. The oil-supply shall be continuous and under pressure. Oil-pumps of ample capacity shall be supplied, capable of maintaining a constant pressure of not less than 5 lbs. per square inch at the bearings. After passing through the bearings the oil shall be passed through strainers into an oil reservoir, from which the oil-pump draws its supply. The oil shall be forced through a thoroughly efficient oil-cooler before being fed to the bearings. An independently-driven oil-pump shall be provided with each turbo-generator for supplying oil during the starting up: this pump shall preferably be steam-driven. A lip shall be cast round the bedplates and bearing pedestals to intercept stray oil. The shaft shall be provided with very efficient oil-throwers, and the whole arrangement within the bearing housings for ensuring against the escape of oil shall be so efficient that after a six hours' run no oil can be detected on the shaft or anywhere outside the housings.

Eddy-currents in the Shaft.

68. The magnetic design of the rotating and stationary parts shall be such that no eddy-current is generated in the journals or bearings, even when the bearings are uninsulated. The bearings shall, however, be so constructed that they can, if need shall arise, be completely insulated from the bedplate and oil-supply; and if there shall be any evidence of the existence of eddy-currents, the insulation of the bearings and all other work necessary to overcome the trouble shall be carried out at the contractor's expense. The bearings shall be provided with suitable arrangements so that their temperature can easily be determined.

Shaft.

69. The shaft shall be of forged steel having a tensile breaking strength of 38 tons per square inch and having an elongation of 18 per cent. measured on a test-piece not less than 3 in. in length and 0·5 in. in diameter. The shaft shall have no sudden variations of diameter. The journals shall be ground and highly polished.

Bedplate.

70. The bedplate shall be of exceedingly stiff construction, and shall be arranged so that either stator may be erected on either bedplate.

Ventilation. 71. The generators shall be completely enclosed and shall be ventilated either by means of a fan on the rotor or by means of an independently driven fan which shall be supplied by the contractor, together with its motor and all necessary auxiliary gear. The motor, if any, for driving the ventilating fan shall be of an approved type. Suitable telltale arrangements shall be provided for warning the switch board attendants in case of any accident to the ventilating arrangements.

Noise. 72. The generators shall not give rise to any more noise than is observable in machines of similar size and speed built according to the best practice.

Cables. 73. The main cables from the armature to the switchboard will be provided by the purchaser under another contract. The contractor shall supply suitable terminals for the armature and field connections and shall supply all necessary cables between the alternator fields and the exciter. He shall also supply any necessary wiring to ventilating motors and other motors, if any, supplied by him for the operation of the plant. After erection the contractor shall examine all connections from the switchboard to the apparatus supplied by him and satisfy himself that such connections are properly made. He shall be responsible for switching-in and paralleling the turbo-alternators with the bus-bars.

Foundations. 74. The purchaser will provide all buildings, foundations, cable ducts and trenches, and floor-plates for the same. The contractor shall supply to the purchaser within four weeks of the closing of the contract proper drawings, templates and materials required to be built into the foundations, so as to enable the purchaser to proceed with the building of the foundations without delay. If through non-delivery of proper drawings, templates or material aforesaid any alterations or additions to the foundations shall become necessary, the cost of the same shall be borne by the contractor. All levelling of the turbo-alternator, bedding and grouting on the foundation shall be done by the contractor.

or,

Framework. 75. The contractor shall supply with each turbo-alternator a steel frame built up of suitable girders of sufficient stiffness to carry the complete turbo-alternator set when placed on the foundations supplied by the purchaser in the positions shown in Figs. 1 and 2. This frame shall be levelled and grouted in by the contractor.

Here may follow clauses Nos. 5, 6, 8 (or its equivalent), 10, 11, 12, 13, 14, 15, 16, 17, 18, 19, 20, 23, 26, or such of them as are suitable.

CALCULATION OF A 15,000 K.V.A. TURBO-GENERATOR.

11,000 Volts, Three-phase, 50 Cycles, 1500 r.p.m.

The calculation given here may seem to be unnecessarily long and complicated. It has been thought desirable to give the reasons for the various stages in the process, and these are sometimes rather lengthy. In actual practice not one quarter of the figuring here shown would be gone through by the designer, because he would make short-cuts based on his experience of previous machines. Nevertheless, the ultimate reasons for the dimensions chosen depend upon some such arguments as those given here.

The design sheet is given on page 387, and the dimensions of the various parts can be scaled off from the drawings given in Figs. 367 to 372.

The method of using the design sheet is in most particulars the same as in the case of the engine-driven generator given on page 316. The main difference arises from the circumstance that the rotor in this case has a distributed winding wound in slots. The machine being totally enclosed and supplied with air from an independent blower, we can make more exact calculations of the air velocity in various parts.

The first point to settle is the diameter of the rotor. For a machine with such a great output we will make this as great as is consistent with maintaining a good factor of safety. A peripheral speed of 18,000 feet per minute is not an excessive speed for a large turbo-generator, so we will try a diameter of 46 inches or 117 cms. The size of the air-gap is fixed from the regulating characteristics, and it will be known from previous machines, or from such considerations as appear later, that it ought to be about $1\frac{1}{4}$ inches, or say, 3·2 cms. This gives us an internal diameter of stator of $48\frac{1}{2}$ inches, or say, 123·4 cms. We may arrive at a preliminary figure for the length by taking a likely D^2l coefficient. If we adopt the type of construction given in Fig. 371, there is no difficulty in making a large four-pole turbo-generator (of 22 per cent. regulation on a load of 0·8 power factor) with a D^2l coefficient no greater than 20,000 inches, or 320,000 cms. This would give us a provisional length of 85 inches. Another way of arriving at the length is to fix upon the number of conductors. A machine of this kind can be worked at about 1000 ampere-wires per inch of perimeter, so we may have about 150,000 ampere-wires. The current per phase is 790, so that the conductors may be about 190 in number. A more convenient number is 180. We can then have 72 slots with five conductors per slot and two paths in parallel. We might, of course, have 60 slots, with three conductors per slot, but this would involve the provision of a conductor to carry 790 amperes, which would be so big that it would have to be stranded, and that would result in a rather weak winding from a mechanical point of view. The extra cost of doubling the number of conductors is such a very small percentage of the total cost of the machine, that it is generally worth while to put two paths in parallel when the current per phase is very great. Another reason for putting two paths in parallel is that it enables us to have 72 slots instead of only 60. Sixty slots would give us 2370 amperes per slot, which, though not an impossible number, is not as good practice as 1970 amperes per slot. We must not, however, get the number of slots too great on a high-voltage machine, or the

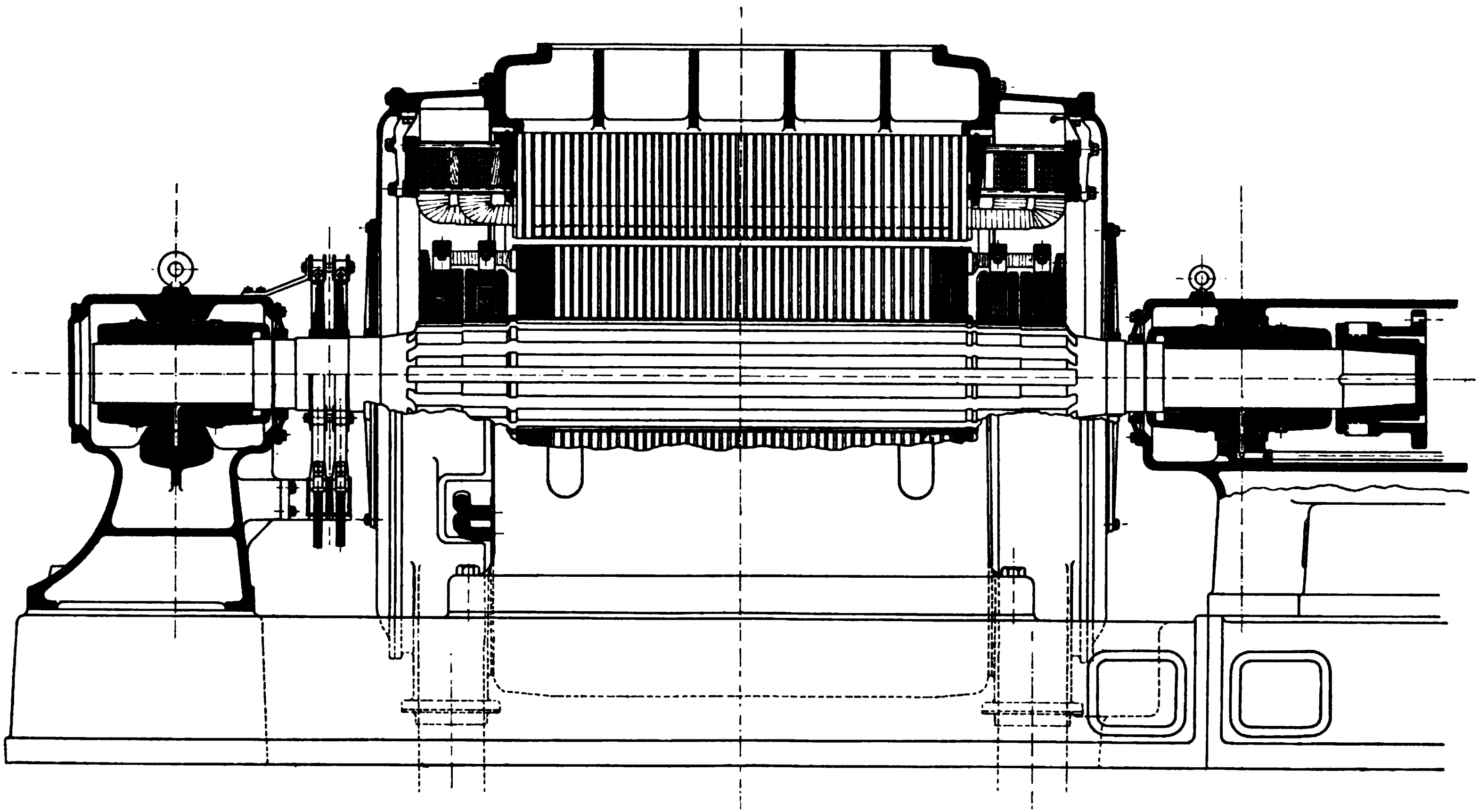


FIG. 367.—Section of 15,000 K.V.A., 3-phase, 11,000 volt, four-pole, turbo-generator, running at 1500 R.P.M. scale 1 : 32.

copper space factor will be very low. With 72 slots we have a slot pitch of 5·35 cms., which is rather small but sufficient. Now find the value of A_gB on the assumption of 180 conductors. We will see (p. 396) that the value of K_e for this type of field is about 0·4. The number of revolutions per second is 25, so we have

$$11{,}300 = 0{\cdot}4 \times 25 \times 180 \times A_gB \times 10^{-8};$$

$$A_gB = 6{\cdot}3 \times 10^8 \text{ C.G.S. lines.}$$

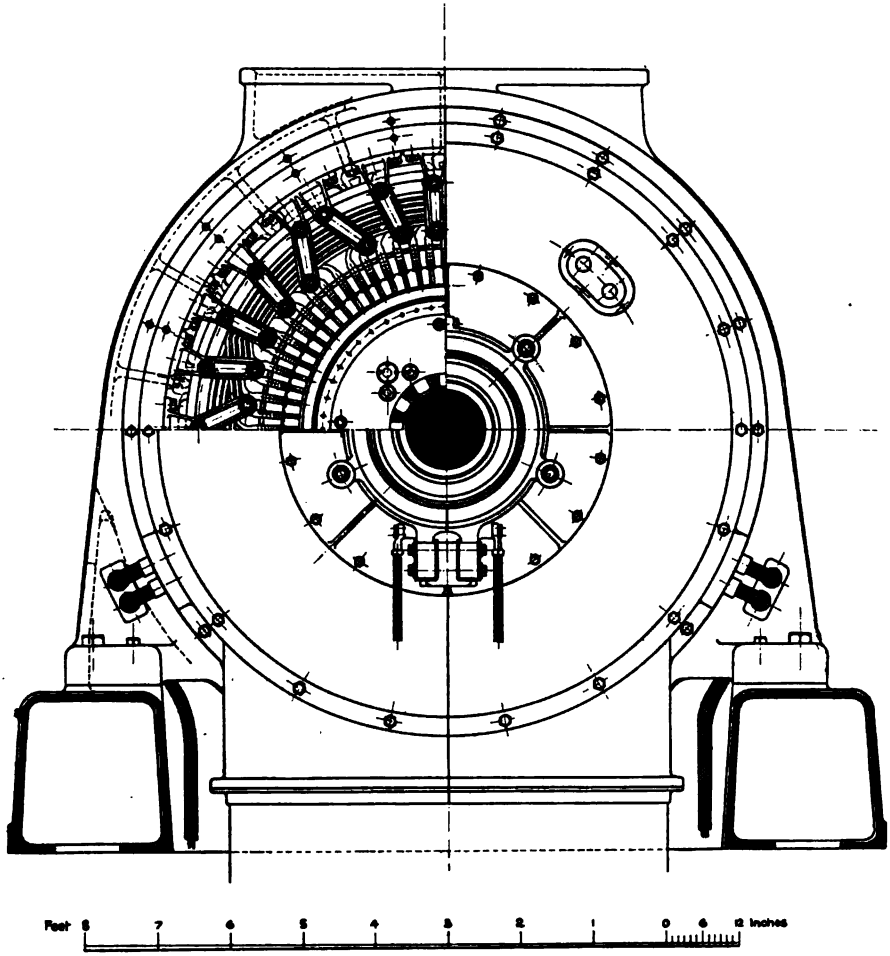


FIG. 368.—End view of 15,000 K.V.A. turbo-generator.

The length of the iron must be great enough to give us room in the rotor to carry this total A_gB, and at the same time provide sufficient copper to carry the requisite ampere-turns on the field. Now the armature ampere-turns per pole are 16,800.

In order to secure good regulation, we should make the ampere-turns at no load some 50 per cent. more than this, and at the same time highly saturate the teeth. We ought, therefore, to have some 26,000 ampere-turns per pole at no load (see page 387). If we work the copper at 3000 amperes per sq. in. and have slots about

4½ ins. deep, we will find that we cannot make the ratio of iron space to copper space much greater than shown in Fig. 371. In this figure we have a tooth 1·5 cms. wide, and a slot with a mean width of 1·7 cms. We have chosen a parallel tooth and taper slot because it is an easy matter to draw the copper strap so as to make good use of the space in a taper slot, whereas a taper tooth is not so economical in room. A taper tooth becomes too highly saturated at the base, while the top is worked at too low a density. A taper slot, moreover, gives us most room near the perimeter, just where it is most useful. We have in this rotor 104 slots, 88 being wound and 16 unwound. If we had made fewer slots, we should have improved the copper space-factor, but, on the other hand, we should have had less cooling surface. The proportions shown are not very far from the best theoretical proportions in this respect, though no doubt the output of the rotor can still be increased by deepening the slots and putting in more copper. With 88 wound slots and six conductors per slot, we get 66 turns per pole. It would be quite practicable with the same type of construction to make eight conductors per slot, and thus reduce the field current, but the space factor would not then be quite so good, and the construction of the conductors would not be quite so robust. An exciting current of 700 amperes is not excessive for so large a generator, and can be easily dealt with if the collector rings and brushes are made ample and well designed. There is some advantage in keeping down the exciting voltage and the number of turns on the rotor, because then the voltage rise in the rotor at the instant of an accidental short-circuit of the stator is not so great.

Having arrived at the size of the rotor teeth, we fix the amount of saturation by considering how many ampere-turns we wish to expend on the iron. In order to give the field distribution the form depicted in Fig. 14, we ought to expend about 20 per cent. of the no-load ampere-turns on the iron. Let us say 5200 ampere-turns on the teeth, which have a length of 10·4 cms., giving us 500 ampere-turns per cm. The apparent flux-density will depend upon the amount of slot and vent space in parallel with the iron; in other words, upon K_s. We may assume a K_s of 2·5 in this first approximation, and from Fig. 47 we find that with 500 ampere-turns per cm. we have an apparent density of 22,500 C.G.S. lines per sq. cm. Dividing this into $6 \cdot 3 \times 10^8$ we get about 27,000 sq. cms. for the area of all the teeth. This gives us a net length of rotor iron of 173 cms., or, allowing for ventilating ducts, say 200 cms. In actual practice the process of provisionally fixing the length would be much shorter than given above. Having fixed the number of slots in the rotor and their width, we would assume a density about 22,500, and arrive at once very near a suitable length. The final adjustment of the saturation can be carried out by changing the number of ventilating ducts, or inserting iron in the empty slots. The length shown in the drawings is 204 cms., so we will proceed with the calculation on that basis. If we have enough room for copper and iron on the rotor, we always find in turbo-generators of good regulation that there is plenty of room for copper and iron on the stator.

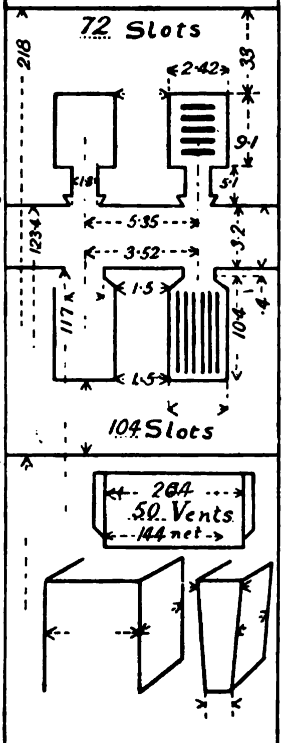
To get room for copper on the stator we have only to choose a slot of sufficient depth. The increase of the self-induction of the armature with the increase in the depth of the slot is, in fact, an advantage rather than a drawback, for the self-induction of the armature of these big machines is generally lower than we wish

Date 6 March 1912 Type Turbo A.C. GEN. ~~SYN MOTOR ROTARY~~ 4 Poles .... Elec Spec. 5
K V A 15,000; P.F. .8; Phase 3; Volts 10,000 to 11,000; Amps per ter. 790; Cycles 50; R.P.M. 1500; Rotor Amps ....
~~H.P.~~ .... Amps p. cond. 395 ~~Amps p. br. arm.~~ By res. Temp rise 45°C ... Regulation 22% P.F.=.8 Overload 25% 4 hours

Customer POWER & LIGHT CO. , Order No... .... , Quot. No............., Perf Spec. ...., Fly-wheel effect ....

Frame 218/204 Circum 388; Gap Area 79000 poss $A_g B$ 7×10⁸; poss $I_a Z_a$ 150,000; $I_a Z_a$; $\frac{D^2 L \times RPM}{K.V.A.} = 3.1\times10^5$
Air 16.5 cu. m. per sec. $A_g B$ 6.3 × 10⁸, $I_a Z_a$ 142,000 ...; Circum. 375 ...,
K_f .4 .. ; 11,300 Volts = .4 × 25 × 180 × 6.3 , Arm. A T. p. pole 16,800 .... Max. Fld. A.T. 188,000

Armature	~~Rev.~~	Stat
Core		
Dia Outs	218	
Dia Ins.	123.4	
Gross Length	204	
Air Vents 50-	0.84	
Opening Min 1.93 Mean	2.5 sq m	
Air Velocity Mean	6.6 m p s.	
Net Length 162 × .89	144	
Depth b Slots	33	
Section 4740 Vol	2.8×10⁶	
Flux Density	11,000	
Loss .045 p cu cm. Total	126,000	
Buried Cu 15,000 Total	171,000	186,000
Gap Area 77,000 Wts	57,000	
Vent Area 2,500,000 Wts	175,000	
Outs. Area 190,000 Wts	19,000	251,000
Teeth		
No of Segs 12 Mn Circ	445	
No of Slots 72 × 2.42 =	175	
K_s 2.34	270	
Section Teeth	38,900	
Volume Teeth	564,000	
Flux Density	16,200	
Loss .08 p cu cm. Total	45,000	
Weight of Iron	26,600 kg	
Conductors		
~~Star or Mesh~~ Throw	6-19; 5-20; &c.	
Cond. p Slot	5/2	
Total Conds	180×2	
Size of Cond 1.28 × 1.4	1.65 sq. cm. × 2	
Amp. p. sq. cm.	240	
Length in Slots 204		
Length outside 240 Sum	4.44 m.	
Total Length	800	
Wt. of 1,000 m 1470 Total	1180 kg	
Res p 1,000 m .0525 Total	.042	
Watts p. metre	98	
Surface p. metre	1950	
Watts p. Sq.	.051	
.0012 × 18° C / 0.4	.054	



.8×3.2×1.04×7800=21000

Field ~~Stat or~~ Rotor.	
Dia. ~~Bore~~	117
½ Total Air Gap	3.2
Gap Co-eff. K_g	1.04
Pole Pitch 94 Pole Arc	
K_f	.66
Flux per Pole	1.04×10⁸
Leakage n.l. 0.064 f.l. 0.114	1.154×10⁸
Area 8400 Flux density	13,800
Unbalanced Pull	
No. of Seg. 1 Mn. Circ.	333
No. of Slots 104 × 1.7 =	177
Vents 50 - .84	156
K_s 2.54 Section	25400 + 1400 = 26800
Weight of Iron punchings	8750 kg.

	Shunt.	Series.	Comm.
A.T. p Pole n. Load	26500		
A.T. p. Pole f. Load	47000		
Surface			
Surface p. Watt			
I² R.	106,000		
I. R.	150		
Amps. 400 n.l.	710 f.l.		
No. of Turns	66		
Mean l. Turn	2(250+66)		
Total Length			
Resistance	.178 cold .21 hot		
Res. per 1,000	.115 and .076		
Size of Cond.	1.5 sq. cm. and 2.25 sq. cm.		
Conds. per Slot	6		
Total	528		
Length	1320 + 349 m.		
Wt. per 1,000	1330 and 2000		
Total Wt.	2460 kg		
Watts per Sq. cm.	.2 in slot		
Star or Mesh			
Paths in parallel			

Magnetization Curve.	Section	Length	10,000 Volts. B.	A.T. p. cm	A.T.	11,000 Volts. B.	A.T. p. cm	A.T.	12,000 Volts. B.	A.T. p. cm	A.T.
Core											
Stator Teeth	38,900	10	14,700	20	200	16,200	40	400	17,700	80	800
Rotor Teeth K_s = 2.54	26,800	10.4	20,800	210	2180	22,800	490	5100	24,500	940	9800
Gap			7150		19,250	7800		21,000	8400		22,700
Pole Body											
Yoke											
					21,430			26,500			33,300

Commutator.
Dia........Speed........
Bars
Volts p. Bar
Brs. p. Arm
Size of Brs.
Amps p. sq.
Brush Loss
Watts p. Sq.

EFFICIENCY	1¼ load.	Full.	¾	½	¼
Friction and W	180	180	180	180	180
Iron Loss	171	171	171	171	171
Field Loss	138	120	110	96	80
Arm. &c. I²R	50	31	18	8	2
Brush Loss					
	539	502	479	455	433
Output	15,000	12,000	9000	6000	3000
Input	15,539	12,502	9479	6455	3433
Efficiency %	96.5	96	95	93	87.5

Mag. Cur Loss Cur.
Perm. Stat. Slot
.. Rot. Slot × =
.. Zig-zag
2 × × =
1.77 × =
End × × =
Amps. Tot.
τ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

it to be. In order that the armature current which will flow, if the machine is accidentally short circuited at full voltage, may be kept within reasonable bounds, it is well that the slot-leakage flux and end-leakage flux of the armature conductors at full load should be equal to about 10 per cent. of the main working leakage (see page 126). In large turbo-generators the main working flux per pole is so great that unless some special provision is made for increasing the permeance of the slot-leakage path, the armature stray field will be only a very small percentage of the whole, and the forces on the armature conductors at the instant of short circuit may be excessive. It is therefore good practice to deliberately increase the slot leakage. This can be done by making the slots of the shape shown in Fig. 370. Incidentally we gain two points of advantage with this construction. We have the armature coils well removed from the rotor, so that there is less fear of a flash between rotor and stator. We provide a very useful cooling surface at the head of every tooth, and allow more air to pass from the ends of the machine to the middle than would be possible with ordinary slots. Observe that it is better to have the mouth of the slot wide and the tooth head fairly long, than to have a narrow mouth and a short tooth head, for though the leakage flux at full load might be the same in both cases, the leakage at ten times full-load current will be more, the wider we make the leakage path. We are, in fact, aiming at providing a leakage path which at ten times full-load current can carry a flux equal to the main working flux without undue saturation.

It is well to work out the leakage flux at full load. This can be done approximately from Figs. 369 and 370 as follows:

The flux passing from the head of one tooth to the head of the next per centimetre length of iron for 1 ampere total current in the slot is

$$1{\cdot}25 \times \frac{5{\cdot}1}{1{\cdot}8} = 3{\cdot}5 \text{ C.G.S. lines.}$$

The effective flux passing across the slot under the same conditions is

$$1{\cdot}25 \times \frac{9{\cdot}0}{3 \times 2{\cdot}42} = 1{\cdot}54.$$

In addition, we have some flux passing along the air-gap in a circumferential direction; this is equal to

$$1{\cdot}25 \times \frac{3{\cdot}2}{5{\cdot}25} = 0{\cdot}77;$$

$$3{\cdot}5 + 1{\cdot}54 + 0{\cdot}77 = 5{\cdot}81.$$

The maximum value of the current in the slot at full load is

$$2\tfrac{1}{2} \times 790 \times 1{\cdot}41 = 2780 \text{ amperes.}$$

The slot-leakage flux per pole is therefore

$$5{\cdot}81 \times 2780 \times 204 \times 2 = 6{\cdot}6 \times 10^6 \text{ C.G.S. lines.}$$

As the working flux amounts to 104×10^6, the slot leakage amounts to $6{\cdot}35\%$.

Next, take the leakage around the ends of the coils. This cannot be calculated with any degree of accuracy. We may employ the formula given on page 426 for the end leakage of induction motors,

$$I_a \phi_e = K_L \times (l_p + a_v) \times \text{virtual A.T. per pole.}$$

The arrangement of the windings most closely resembles the case where we have a concentric winding on the stator and a squirrel cage winding on the rotor, so that K_L, from Table XVIII., p. 427, is 2·8. Taking $l_p = 111$ cms. and $a_v = 28$ cms., we have for the full load ampere-turns, 11,850,

$$2{\cdot}8 \times (111 + 28) \times 11{,}850 = 4{\cdot}6 \times 10^6 \text{ C.G.S. lines per pole.}$$

Adding the slot leakage, we get

$$(6{\cdot}6 + 4{\cdot}6)10^6 = 11{\cdot}2 \times 10^6,$$

or approximately 11 % of the working flux.

The heads of the teeth are made wider than the body of the teeth by an amount sufficient to give mechanical support to the coils. One advantage of wide heads is that the iron loss is lower than if the heads were made narrow and long. A long head, on the other hand, brings the armature slots on a larger diameter, and allows a rather wider slot to be used than would be otherwise possible.

In fixing upon the size of slot, it must be remembered that plenty of room must be allowed for insulation between turns. Although the normal running voltage between successive turns is only 110, the insulation should be able to resist a puncture test of 4000 volts. A good plan is to place a strip of micanite 1 mm. thick between each turn, and in addition to this there will be two layers of half-lapped linen tape, so we must add 1·5 mm. to the depth of each conductor. The current per conductor is 395 amperes. The size of conductor that we must employ will depend upon the cooling conditions. Here we have 11,000 volt insulation and a high current per slot, so it will be found that we cannot work at a high-current density. To find the permissible current density, we must make a rough guess at the cooling conditions. We know that the cooling surface of the coil in the slot will be about 2000 sq. cms. per metre length. Suppose that we allow 18° C. temperature difference between the inside and the outside of the coil. With the teeth at 50° C. that would mean a temperature of 68° C. for the copper. The insulation will be about 0·4 cm. thick, and the heat conductivity 0·0012 watt per sq. cm. per degree per cm.

$$\text{Permissible watts per sq. cm.} = \frac{0{\cdot}0012 \times 18}{0{\cdot}4} = 0{\cdot}054.$$

This allows us 100 watts per metre length of coil, and as we have 5 conductors, each carrying 395 amperes, it is easy to calculate that the resistance per metre when hot should not be more than 0·000128 ohm. If we choose a conductor with a cross-section of 1·65 sq. cms. it will be about right. This has a resistance of 0·105 ohm per 1000 metres at 20° C., so allowing for 50° C. rise we have the watts per metre length of coil

$$395 \times 395 \times 0{\cdot}000105 \times 1{\cdot}2 \times 5 = 98 \text{ watts.}$$

The actual mean perimeter of the insulation works out at 19·5 cms., so that the cooling surface is 1950 sq. cms. per metre, giving the required sq. cm. per watt.

Having obtained our cross-section, the next point is to fix on the external dimensions. We would, in practice, be guided in this by considering what slot dies we had available, but in the absence of any such consideration we will make the width of the strap as great as we can, so that the depth may be as small as possible. In this case we cannot make the width greater than 1·4 cms. or we will

make the teeth too narrow. So our conductor, if in one piece, would be 1·4 by 1·28. Now, we see from Fig. 167 that if we used a solid conductor as deep as 1·28 cms. near the mouth of the slot, the eddy-current loss on it would be very excessive. We have $a=0{\cdot}78$ and $f=1{\cdot}28$, so that $af=1$. Now, from the curve $m=5$, we find that the loss in the top conductor will be 7·5 times as great as it should be. We have therefore made the top conductor and the one next to it of stranded copper, and the rest of the conductors we have divided into two parts, slightly insulated from each other. All the end connectors we have made of double straps, twisted on themselves midway along their length, as shown in Fig. 370. There are two objects in twisting the end connectors. In the first place, they interconnect the top and bottom halves of conductors lying in different slots, and so neutralize the eddy current which would otherwise circulate in these two halves. Secondly, by the twist we neutralize to a great extent the eddy current which would be generated in each end connector itself. The total maximum current in all the end connectors amounts to 29,000 amperes. This will set up a very strong field in the body of the copper connectors, and it is desirable that they should be laminated as much as possible. Stranded copper connectors would be better electrically than the straps shown in Fig. 370, but they would be rather weak mechanically.

Thus, we arrive at the arrangement of conductors shown in Figs. 369 and 370. Taking into account the requisite insulation (see page 201) between turns and the outside insulation, we arrive at the dimensions of the slot shown. It will be seen that, in addition to the retaining wedges at the mouth of the slots proper, there are wedges bridging across between the heads of the teeth. These are to prevent excessive noise and churning of the air. It is best to make these wedges in short pieces, each no longer than the thickness of a packet of iron punchings, so that the air has easy access to the cooling surface afforded by the heads of the teeth. The width of the conductor has been chosen so that sufficient iron is left in the teeth. This cannot be finally checked until the number and size of the ventilating ducts is fixed.

A good rough rule for settling on the number of ventilating ducts on big turbo-generators is to allow one duct for every 1½ inches of iron on a 50-cycle generator, and one duct for every 2 inches of iron on a 25-cycle generator (see page 253). The size of the ducts will depend on the amount of air that must be put through the machine, and the pressure available. Where it is intended to employ an independent blower to give the air supply, fairly narrow ducts can be used, as it is a very simple matter to increase the air pressure, if it is found that too little air is passing. A handy formula for calculating the amount of air required is the following:

$$\text{Cubic metres of air per second} = \frac{0{\cdot}85 \text{ K.W. loss}}{\text{temperature rise of air, } ^\circ\text{C.}}$$

Or, in other units,

$$\text{Cubic feet per minute} = \frac{1{\cdot}78 \times 10^3 \times \text{K.W. loss}}{\text{temperature rise of air, } ^\circ\text{C.}}$$

In these formulae the volume of the air is supposed to be measured at 20° C. At 60° C. the volume will be 14 % greater.

We know from previous experience that the losses in the 15,000 K.V.A. generator will be about 500 K.W. If we allow an average temperature rise of 25° C. for all the air going through, we have

$$\frac{0{\cdot}85 \times 500}{25} = 17 \text{ cubic metres per second.}$$

On the calculation sheet it will be seen that we have allowed 16·5 cubic metres per second. This is equal to 35,000 cubic feet per minute.

Velocities of air in various parts. If, now, we take 50 ventilating ducts, each 0·8 cm. wide, we will have, half-way between the internal and external diameters of the stator, a total area of path of 2·5 sq. metres. With an air supply of 16·5 metres per second, we will have a mean velocity of 6·6 metres per second. This is quite a suitable velocity. The minimum opening of the ventilating ducts (that is, near the slots) is 1·03 sq. metres, giving a velocity of 16 metres per second. This is fairly high, but not excessive. The total area available for the passage of air in an axial direction, along the air-gap and along the rotor ducts and empty slots, is about 0·5 sq. metre, so that the velocity of the air entering the air-gap at each end will be about 30 metres per second, and the velocity along the rotor ducts will be rather higher than this.

Having settled on 50 ventilating ducts, each 0·84 cm. wide, we get by subtracting 42 from 204, 162 cms. of punchings and paper. Multiplying by the factor 0·89, we get 144 cms. of solid iron.

Flux-density in the teeth. We are now in a position to check the cross-section of all the teeth. As explained on page 322, we find the maximum density in the teeth by merely dividing the total A_gB by the cross-section of all the teeth. Imagine a circle drawn through all the teeth, which has a diameter of 142 cms. It has a circumference of 445 cms. This we call on the calculation sheet Mn. circ., or mean circumference. From this must be subtracted the sum of the widths of all the slots, or $72 \times 2{\cdot}42 = 175$. This leaves us 270 cms. of iron all the way round. Multiply this by 144, and we get the total mean section of the teeth as 38,900 sq. cms. Divide $6{\cdot}3 \times 10^8$ by this, and we get 16,200 for the mean flux-density on the teeth. This is rather high for a big generator, but not too high. The volume of the teeth is obtained by multiplying the section by the length. The loss per cu. cm. can be obtained by referring to Fig. 29. For very special machines, however, we can, as explained on page 52, by extra care make the iron loss considerably less than that given by the curves on Fig. 29. In this case we take the loss on the teeth at 0·08 watt per cu. cm., which gives us a total loss on the teeth of 45 K.W.

Depth below slots. This dimension depends upon the total flux per pole. Dividing $6{\cdot}3 \times 10^8$ by 4, and multiplying by the form factor $K_f = 0{\cdot}66$, we get the working flux per pole $1{\cdot}04 \times 10^8$ C.G.S. lines. If we allow a flux-density of 11,000 per sq. cm., we shall require in each half of the path for this flux 4740 sq. cms. Dividing 4740 by 144, we get 33 cms. for the required depth of iron.

This depth is often fixed in practice by the bore of some existing frame, and sometimes the flux-density will be higher or lower than 11,000 to fit existing parts. Where no such restriction exists, one employs a density of 11,000 for 50-cycle generators and 12,000 for 25-cycle generators. The volume of the iron behind the slots is obtained by multiplying the area 4740 by the mean circumference 585 cms.

This gives us $2{\cdot}8 \times 10^6$ cu. cms. We may take the loss at 0·045 watt per cu. cm., so that the loss behind the slots is 126 K.W. Adding this to the tooth loss, we get 171 K.W. The total length of armature coils buried in the iron is

$$72 \times 2{\cdot}04 = 147 \text{ metres.}$$

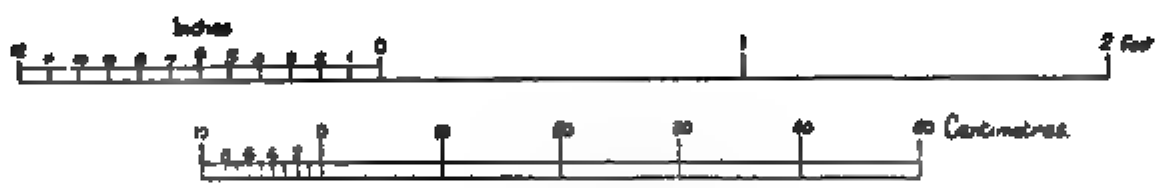


FIG. 369.—Section of end winding of 15,000 K.V.A. turbo-generator, showing the clamp bolted to the external ring of fender.

Multiply this by 98 watts, and we get 14·5 K.W. for the buried copper losses. The total losses to be dissipated by the surface of the stator is therefore 185 K.W.

Cooling of the stator. We have now to consider how we will get rid of all the heat generated by this lost power.

First, take the inside cylindrical surface or gap-area. This is 77,000 sq. cms. in area.

$$\text{Watts per sq. cm.} = \text{temperature above air} \times \frac{(1 + 0{\cdot}1v)}{333}.$$

FIG. 370.—View of end-winding, showing the twin conductors twisted on themselves to prevent eddy currents. Also section showing vent-plate.

It is shown below that the mean-temperature rise of the air in the air-gap will be about 16° C. Taking the iron at 40° rise, we have a difference of 24° C.

$$v = 92 \text{ metres per second.}$$

Therefore watts per sq. cm. $= 0{\cdot}74$,

$$77{,}000 \text{ sq. cm.} \times 0{\cdot}74 = 57{,}000 \text{ watts.}$$

Next, take the cooling from the walls of the ventilating ducts. The total area, counting both sides of the ducts and allowing for spacers, is about 2,400,000 sq. cms. The mean velocity in the ducts is 6·6 metres per second.

$$h_v = 0{\cdot}0007 \times 6{\cdot}6 = 0{\cdot}0046.$$

Before we can estimate the watts per sq. cm. dissipated by the surfaces of the ventilating ducts, we must find the mean-temperature rise of the air in the ducts. We must first ask what amount of heat is received by the air before it enters the ducts. We have

Field I^2R loss $= 106$ K.W. (see p. 387).
Windage of rotor $= 125$
Heat from inside stator $= 57$
Armature connectors $= 17$

$= 305$ K.W.

$$\frac{0{\cdot}85 \times 305}{16{\cdot}5} = 15{\cdot}7^\circ \text{ C.}$$

mean rise of temperature of air before entering the ventilating ducts. Now, as we only have between 100 and 120 K.W. to get rid of from the ventilating ducts, this will make a further rise of

$$\frac{0{\cdot}85 \times 120}{16{\cdot}5} = 6^\circ \text{ C.}$$

The mean temperature rise of the air after it has passed half-way through the ducts is 19° C. above the outside temperature. If, now, we take the temperature of the surface of the ducts at 35° rise, we have a difference of 16° between air and iron.

$$\text{Watts per sq. cm.} = 16\,; \qquad h_v = 16 \times 0{\cdot}0046 = 0{\cdot}074,$$

$$0{\cdot}074 \times 2{\cdot}4 \times 10^6 = 175{,}000 \text{ watts.}$$

We have therefore very much more cooling surface than is necessary to carry away the heat generated from the calculated losses. In fact, the mean temperature rise to be expected is only $19^\circ + 6^\circ = 25^\circ$ C. We must, however, remember that the temperature in the middle of the machine may be some 5° or even 10° hotter than at the ends, and the iron loss may be somewhat higher than we have calculated; therefore the margin which we have allowed is desirable.

A certain fraction of the heat generated is dissipated by the end plates and conducted into the frame, whence it passes to the air circulating through the frame. For large turbo-generators, we may allow about 0·1 watt per sq. cm.* of external surface for the heat dissipated in this way. The external surface is 190,000 sq. cms., so we have about 19 K.W. dissipated by conduction into the iron frame and to the end plates.

* The reason for allowing a smaller rate of cooling on the external surface than on slow-speed machines is that the ratio between the surfaces through which the heat is conducted and the whole external surface is less than on slow-speed machines.

The various quantities of heat dissipated from the gap-area, the vent-area and the outside area for a temperature rise of 40° C., are entered in their respective places on the calculation form (page 387). The sum is 251 K.W. As the calculated loss to be dissipated from the iron parts of the stator is 186 K.W., the temperature rise to be expected is lower than 40° C.

The other figures for the armature given on the calculation sheet explain themselves.

The length of the air-gap is fixed so as to give us about 21,000 A.T. per pole, that is to say, some 25 % greater than the armature ampere-turns per pole. The maximum flux-density in the gap is obtained by dividing the gap area 79,000 * into $6 \cdot 45 \times 10^8$. This gives us 8170 C.G.S. lines per sq. cm. We then have

$$0 \cdot 796 \times 3 \cdot 2 \times 1 \cdot 04 \times 8170 = 21,600 \text{ ampere-turns on the gap at 11,600 volts.}$$

As the air-gap is so great in comparison with the opening of the slots and ventilating ducts, the gap coefficient is nearly unity. The working flux per pole is obtained by dividing $6 \cdot 3 \times 10^8$ by 4 and multiplying by 0·66 the form factor K_f. In making out the tables for the magnetization curve, it should be remembered that the maximum density in the gap is not quite proportional to the voltage, because the coefficient K_e changes slightly as the saturation increases. In practice we change the constant by a small amount, which can be judged from experience. If we wanted to be very accurate, we would have to make a plot of the field-form at two or three voltages, as shown in Fig. 373, and determine K_e.

In taking the cross-section of the teeth, we must not forget the area of the spacers in the ventilating plates. These have a total section of 1400 sq. cms., making total section of iron 26,800 sq. cms. If we multiply the mean circumference 333 by the total length 204, we get the total section of air and iron, and dividing this by 26,800 we get $K_s = 2 \cdot 54$. The high value of K_s makes the apparent flux-density in the teeth very much higher than the actual, and has a great influence on the number of ampere-turns required for the teeth, as can be seen from Fig. 47. If we work out the ampere-turns per pole for various flux-densities in the air-gap, we arrive at the figures given below :

B in air-gap.	Apparent B in teeth.	Ampere-turns per cm., $K_s = 2 \cdot 54$.	Ampere-turns on teeth.
6,000	17,200	60	625
7,000	20,500	200	2,080
8,000	23,000	600	6,250
9,000	25,800	1,350	14,050
10,000	28,750	2,300	24,000

For B = 8000 in the gap, we have

$$0 \cdot 796 \times 3 \cdot 2 \times 1 \cdot 04 \times 8000 = 21,050 \text{ ampere-turns on gap.}$$

This gives us the position of the air-gap line shown dotted in Fig. 373. From this line we set off the ampere-turns on the teeth as described on page 78, and obtain the " air-gap and tooth " saturation curve. In order to find the field-form

* The gap-area for this purpose is taken at 79,000 to allow for the fringing at the ends of the rotor.

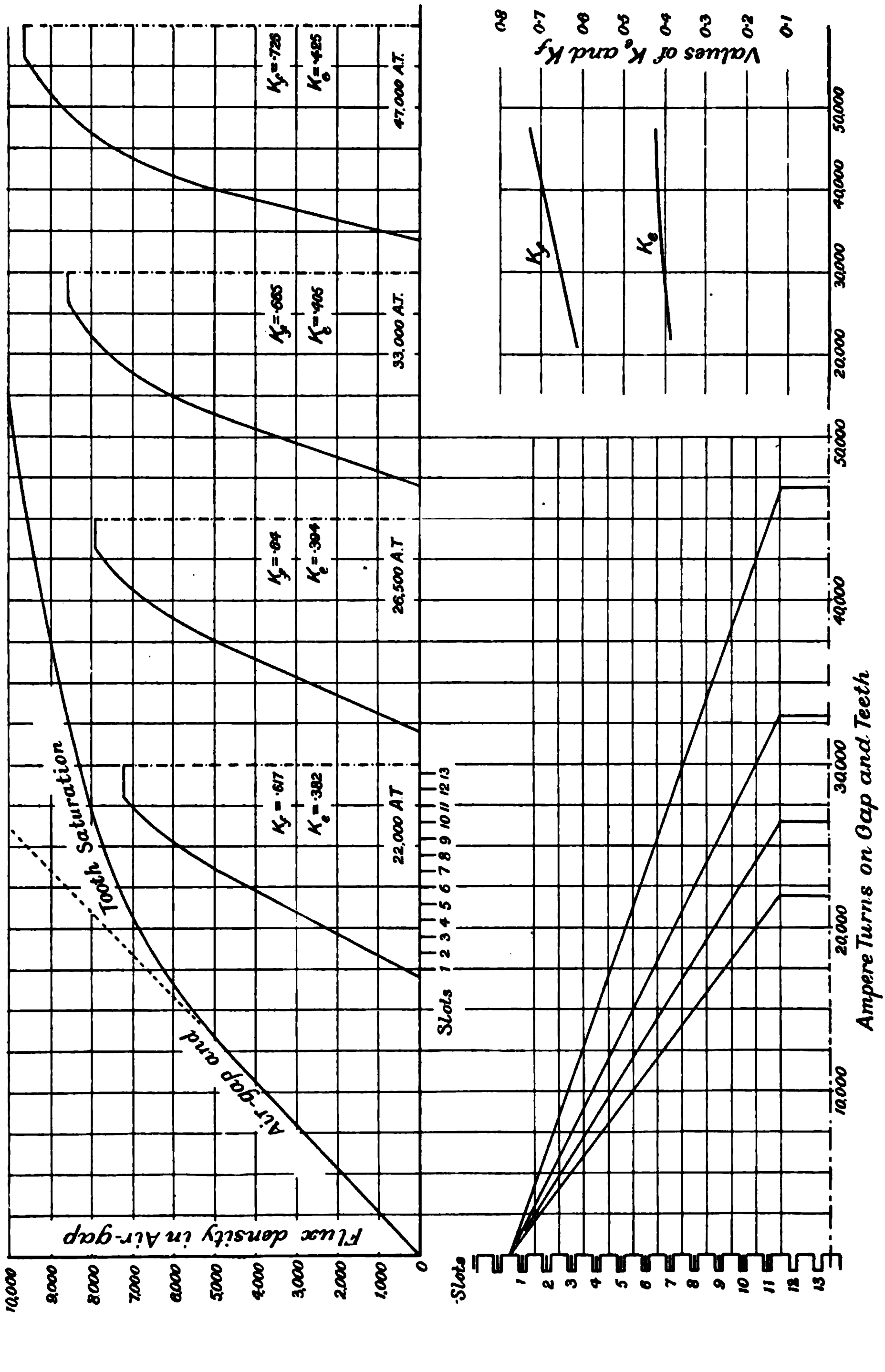


FIG. 378.—Method of finding the field-forms at various excitations on 15,000 K.V.A. generator.

at various excitations, we set off the trapeziums which give the distribution of magnetomotive force (see page 375). These are shown at the base of Fig. 373 for 22,000, 26,500, 33,000 and 47,000 A.T. respectively. Running up the ordinates for the ampere-turns on each tooth until we strike the " air-gap and tooth " curve, and then along horizontally as shown in Figs. 366 and 373, we can plot the field-forms shown. By means of a planimeter we at once find K_f, and K_e can be found by the method described on page 28. Another way of arriving at K_e is to take the value of the voltage coefficients as determined by Dr. S. P. Smith,* and find its ratio K_K to the voltage coefficient for a sine-wave field-form. Now K_e for a sine-wave field-form is 0·39 (see page 25), so that $0{\cdot}39 \times K_K = K_e$.

It will be found that, for field-forms of the general shape of those shown in Fig. 373, there is a fairly close relation between the value of K_e and K_f, so that after we have worked out a number of cases we can plot a curve giving the relation as

9

FIG. 374.—Curve showing relation between K_e and K_f for field-forms of the general shape shown in Fig. 373 in star connected 3-phase machines.

shown in Fig. 374. It is then only necessary to find K_f by means of a planimeter (see page 16), and read off the value of K_e from Fig. 374. The change in the values of K_f and K_e as the excitation is changed will be seen from the curves plotted at the right-hand side of Fig. 373. Knowing the maximum values of B for various excitations, and the values of K_e, we can now find the voltage by means of the formula

$$\text{Volts} = K_e \times R_{ps} \times \text{No. of conductors} \times A_g \times \text{B}.$$

For instance, at 22,000 volts, we have

$$\text{Volts} = 0{\cdot}382 \times 25 \times 180 \times 79{,}000 \times 7200 = 9800.$$

We can now plot the no-load magnetization curve as shown in Fig. 375.

The amount of iron in the rotor teeth and the length of the rotor teeth are adjusted so as to absorb about 20 % of the ampere-turns per pole at no load. In this machine 5100 ampere-turns are absorbed on the teeth at 11,000 volts. This amount is so great that the ampere-turns absorbed by the armature teeth and

* " The Non-salient Pole Turbo-Alternator and its Characteristics," *Journ. I.E.E.*, vol. 47, p. 562.

core can in general be neglected. We, however, give the figures for the armature teeth on the calculation sheet, though the possible error in the figures for the rotor teeth make these small figures of little value.

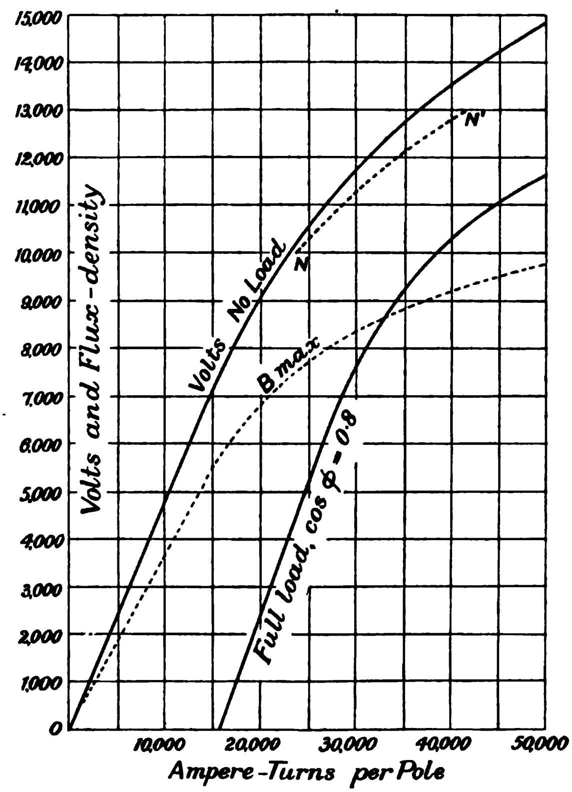


FIG. 375.—No-load and full-load magnetization curves of 15,000 K.V.A. generator.

The field leakage depends mainly upon the permeance of the field slots. Applying the ordinary rules, we find that 1 ampere passing in a slot creates about 5·5 C.G.S. lines per cm. length. At 400 amperes we have

$$400 \times 6 \times 5{\cdot}5 \times 204 \times 2 = 5{\cdot}4 \times 10^6.$$

To this should be added about 1×10^6 C.G.S. lines for end leakage.

It will be seen from Fig. 371 that some wedge-shaped pieces of iron have been inserted in the slots at the sides of each pole. These iron wedges are so proportioned that they will carry the no-load leakage when saturated to the same extent as the teeth are saturated by the working flux. There is therefore no increased saturation due to leakage at normal-voltage no-load. At full load, however, the leakage is increased to $1{\cdot}14 \times 10^7$ C.G.S. lines per pole; the difference 5×10^6 so highly saturates the teeth in the centre of the pole that there would be required an increase of 4000 in the ampere-turns on the teeth from this reason alone, were it not for the change in the value of K_e. By plotting the field-form under the new con-

ditions by a process of trial and error, it will be found that, with the ampere-turns increased to 47,000 per pole, the value of K_e goes up to 0·425, and this reduces the extra ampere-turns required for the centre teeth to 2500. By taking two or three points on the saturation curve, and investigating in this way the effect of the increased saturation, we get the dotted curve NN' for the magnetization curve with increased saturation on load.

Having obtained this curve NN', the plotting of the full-load magnetization curve is carried out exactly as described on page 386, and is given in Fig. 375. We find that with an inductive drop in the armature of 10 % (see p. 389) it is necessary to generate 11,700 volts in order to get 11,000 volts at the terminals at full load, 0·8 power factor. Taking the ampere-turns required for 11,700 volts from the curve NN', and compounding these as in Fig. 305 with the 15,500 ampere-turns of the armature, we arrive at 44,500 ampere-turns per pole at full load, 0·8 power factor. It is well to allow some margin on this to allow for the iron being more highly saturated, as would be the case if the punchings were not very tightly packed. We have taken 47,000. This gives us an exciting current at full load of 710 amperes.

The calculation of the cooling of the copper in the rotor slots is straightforward. The area of the strap in the slot is 1·5 sq. cms., so that the resistance of 1 metre of the conductor is 0·000115 cold. We have, therefore,

$$0{\cdot}000115 \times 1{\cdot}16 \times 710^2 \times 6 = 400 \text{ watts per metre.}$$

The area of the insulation is about 2000 sq. cms. per metre and the thickness 0·15 cm.

$$\frac{0{\cdot}0014 \times t^\circ}{0{\cdot}15} = \frac{400}{2000},$$

$$t^\circ = 21{\cdot}5^\circ \text{ C. rise of copper above iron.}$$

It is interesting to note that with this construction we can work the copper in the slot as high as 470 amperes per sq. cm., and yet have quite a low temperature rise.

The area of the end connectors of the rotor must be greater than the area of the conductors on the slots, on account of the much poorer cooling conditions. We have chosen an area of 2·25 sq. cms. The cooling takes place, partly by conduction of the heat through the insulation flanking the end connectors, and partly by conduction along the connectors to the ends which are very well ventilated. The general method of finding the temperature rise in cases of this kind is described on page 226. This case is rather complicated by the fact that the connectors are reduced in section at the dovetailed portion, and the flow of heat by conduction along the copper is throttled at this point. The simplest way of getting over this difficulty is to imagine the conductors are not reduced in section, but that they are lengthened instead. It will be seen that both I_d, the current density, and x, the length of the conductor, enter into the equation

$$T_x = T_{\max} \cos (4{\cdot}71 \times 10^{-5} \times I_d \times x)$$

in such a way that to multiply I_d by any constant has the same effect as multiplying x by the same constant.

For instance, on the machine under consideration, in the part 2 cms. long, where the cross-section is reduced to one-third, and the current density is increased

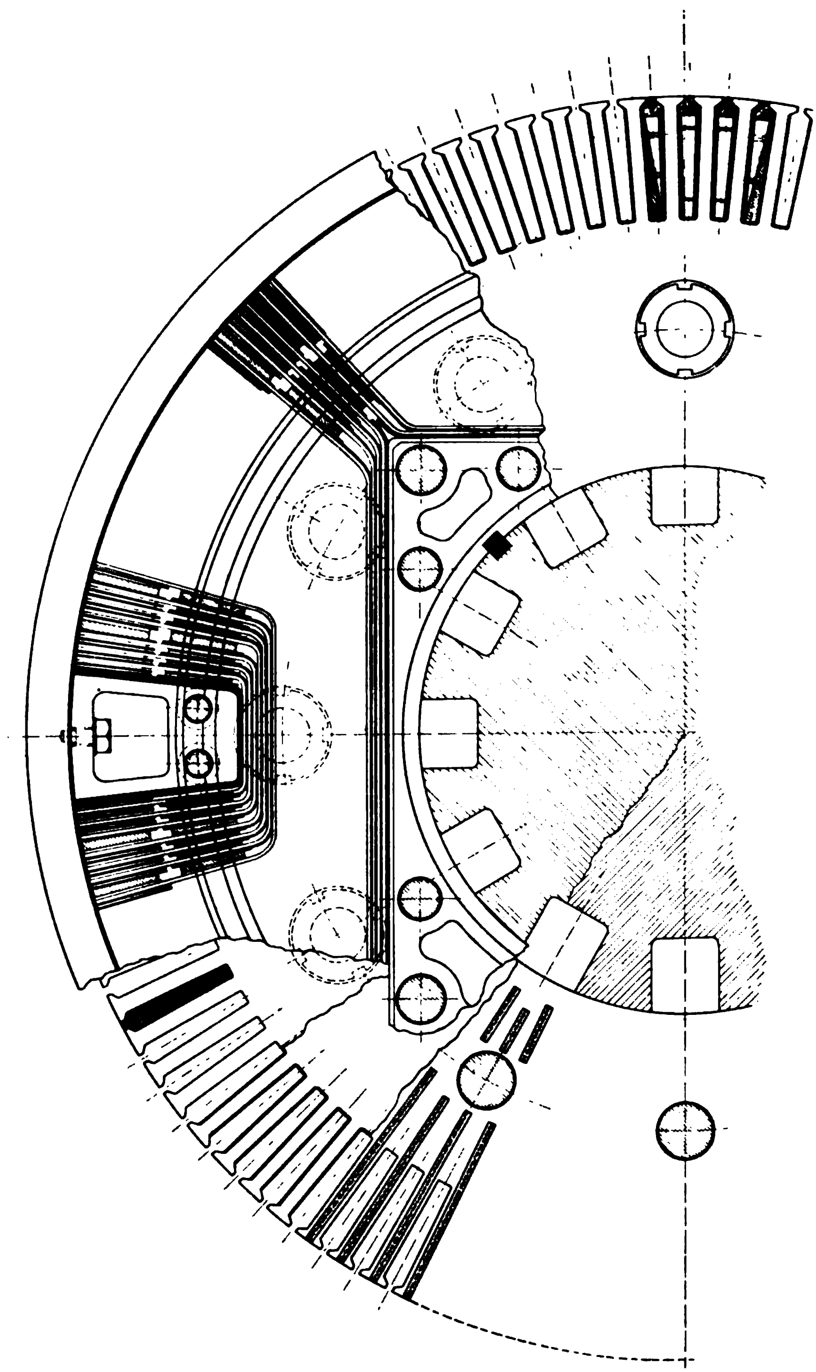


FIG. 371.—Showing various sections through the revolving field-magnet of a 15,000 K.V.A. turbo-generator, having U-shaped end-connectors.

Fig. 372.—Showing section through steel cheeks supporting U-shaped end-connectors by means of mica V-rings

to three times, the temperature fall will be the same as if the section had not been reduced, but instead, the part in question were made 6 cms. long. If we take into account the bevelled part, we find that the effect of the whole reduction in section is the same as adding 4·5 cms. to the length. This makes the strap, which we may judge to be the hottest, about 37 cms. from its centre point to the place where there is presented a large cooling surface.

The watts lost in the 33 end-connectors for one pole, at a current of 710 amperes, are 960 watts. From this we must subtract the watts dissipated by conduction through the insulation. The cooling surface of 2400 sq. cms. at say 0·13 watt per sq. cm. gets rid of 310 watts. $960 - 310 = 650$ watts to be conducted along the copper.

$$\frac{I_v^2}{I_d^2} = \frac{650}{960}; \quad \therefore \; I_v = 0{\cdot}82 I_d.$$

$I_d = 315$ amperes per sq. cm.; $\therefore$ $I_v = 258$ amperes per sq. cm.

The law of temperature distribution is

$$T_x = T_{\max} \cos (4{\cdot}71 \times 10^{-5} \times 258 \times 37).$$

Now, the very large cooling surface exposed by the straps which pass over from one tier to the next, and the strong blast of air blowing on this surface, will keep the ends of the connectors very cool. 20° C. rise is an outside figure for the arrangement shown. Let us say that the actual temperature of the ends is 45° C. Add 240° (see p. 227), and we get

$$285 = T_{\max} \cos 0{\cdot}45,$$

$$T_{\max} = 316,$$

$$316 - 240 = 76° \text{ C. actual},$$

or, say, 51° C. rise in the hottest point of the connectors.

In calculating the resistance of the field winding we find that the bars have a total length of 1320 metres, and have a resistance of 0·115 ohm per 1000 metres, while the end connectors have a total length of 349 metres of a conductor having a resistance of 0·076 ohm per 1000 metres. The total resistance is 0·178 ohm cold, or say, 0·21 ohm hot. To drive 710 amperes we will require 150 volts, so that the exciter should be capable of generating about 190 volts to deal with over loads.

The working out of the efficiency will be easily followed from the calculation sheet.

TWO-POLE TURBO-GENERATORS.

In order to get the high steam economy which is only possible at very high speeds, the tendency is to build larger and larger units running at 3000 R.P.M. Generators of 50 cycles, having an output as high as 5000 K.V.A., are now run at this speed. Such a high speed does not lead to economy in the generator itself, because the windage losses are high and the cost of construction is greater than for a four-pole generator of half the speed. These disadvantages, however, are outweighed by the advantages to be gained in the steam turbine. It has therefore been necessary to overcome the inherent difficulties in building a two-pole turbo field-magnet, and this has been satisfactorily accomplished by the constructions shown at the beginning of this chapter.

In order to get as high an output as possible from a machine of limited diameter and length, these high-speed, two-pole machines are usually made with a rather low ratio of field ampere-turns to armature ampere-turns, so that the regulation is very poor. Automatic regulators are therefore commonly used in conjunction with them to keep the voltage constant. Very often no guarantee is given as to inherent regulation, but an automatic regulator is supplied which will hold the voltage within 1 or 2 per cent. under normal working conditions.

For the 2500 K.V.A., 2-pole, 50-cycle generator, particulars of which are given on the design sheet, page 406, we have chosen a rotor cut out of a solid steel forging, because this construction enables us to make the critical speed at which the rotor begins to whip, higher than the running speed. The performance specification might be worded as in Specification No. 6.

SPECIFICATION No. 6.

2500 K.V.A. THREE-PHASE GENERATOR TO BE DRIVEN BY A STEAM TURBINE AT 3000 R.P.M.

Clause as to General Conditions, see Clauses 1, 21, 170.

Extent of work.

80. The work includes the supply, delivery, erection and setting to work at , of a turbo-generator and exciter, together with automatic regulating gear. The plant shall have the following characteristics:

Characteristics of Generator.

Normal output	2500 K.V.A. or 2000 K.W.
Power factor of load	0·8.
Number of Phase	3.
Normal voltage	550.
Voltage variation	520 to 570.
Amperes per phase	2620.
Speed	3000 revs. per minute.
Frequency	50 cycles per second.
Regulation	The generator or its exciter shall be controlled by an automatic regulator, which shall keep the voltage constant within 1 per cent. when a load of 200 K.W. at 0·8 power factor shall be thrown on or off the generator. This regulator shall be supplied * under the contract for the supply of the generator, and shall be included in the price.
Over load	3300 amperes per phase at 550 volts with power factor between 0·9 and unity.
Exciting voltage	110.
Temperature rise after 6 hours full load	40° C. by thermometer. 55° C. by resistance.
Temperature rise after 2 hours over load	55° C. by thermometer. 70° C. by resistance.

* In some cases the purchaser will already have a regulator installed. In these cases particulars should be given of the type and arrangements made for including the new exciter in the regulating scheme.

81. The generator is intended to supply power to two cotton factories situated at , and to three other factories at a distance of about 1 mile. Some 1250 K.W., taken from the generator at 550 volts, will be transformed up to 3000 volts for transmission by underground mains to the three distant factories; part will be consumed without transformation, on motors varying in size from 5 H.P. to 100 H.P., and another part, about 100 K.W., will be transformed by static balancers to 120 volts for lighting. This lighting load will be distributed as evenly as may be between phases, but the phases may be sometimes slightly out of balance. The generator must be suitable in every way for this class of work. **Nature of load.**

82. The revolving parts of the generator and exciter shall be so constructed that the critical speed is not less than 3600 revs. per minute. **Critical speed.**

83. At the normal speed of 3000 revs. per minute the rotor shall have a calculated factor of safety on every part of not less than four. The revolving part shall, before leaving the Contractor's works, be run at a speed of 3300 revs. per minute, without showing signs of movement of the component parts relatively to one another. **Factor of safety.**

Here may follow Clauses Nos. 5, 6, 8 or its equivalent (see Clauses 55 to 59), 10, 11, 12, 13, 14, 15, 16, 17, 18, 19, 20, 23, 26, 27, 60, 61, 64, 66, 68, 69, 70, 73, 74, or such of them as are suitable for the case.

CALCULATION OF A 50-CYCLE 2500 K.V.A. TURBO-GENERATOR RUNNING AT 3000 R.P.M.

Diameter of rotor. The considerations which determine the diameter of the rotor are as follows. The smaller the diameter the less will be the centrifugal forces and the less will be the windage. On the other hand, a small diameter may necessitate a great axial length in order to get the required output, and a great length makes it difficult to give to the rotor sufficient lateral stiffness. The critical speed at which the rotor begins to whip depends upon the stiffness of the rotor regarded as a beam supported at its two bearings. The critical speed in revolutions per minute is equal to

$$\frac{60}{2\pi}\sqrt{\frac{32{\cdot}2 \times 12 \times (W_1y_1 + W_2y_2 + W_3y_3 + \text{etc.})}{(W_1y_1^2 + W_2y_2^2 + W_3y_3^2 + W_4y_4^2 + \text{etc.})}}$$

where W_1, W_2, etc., are the weights (in lbs.) of various convenient sections of the rotor and y_1, y_2, etc., are the deflections (in inches) of the centres of those sections produced by the action of gravity as the rotor is held horizontally on its bearings.

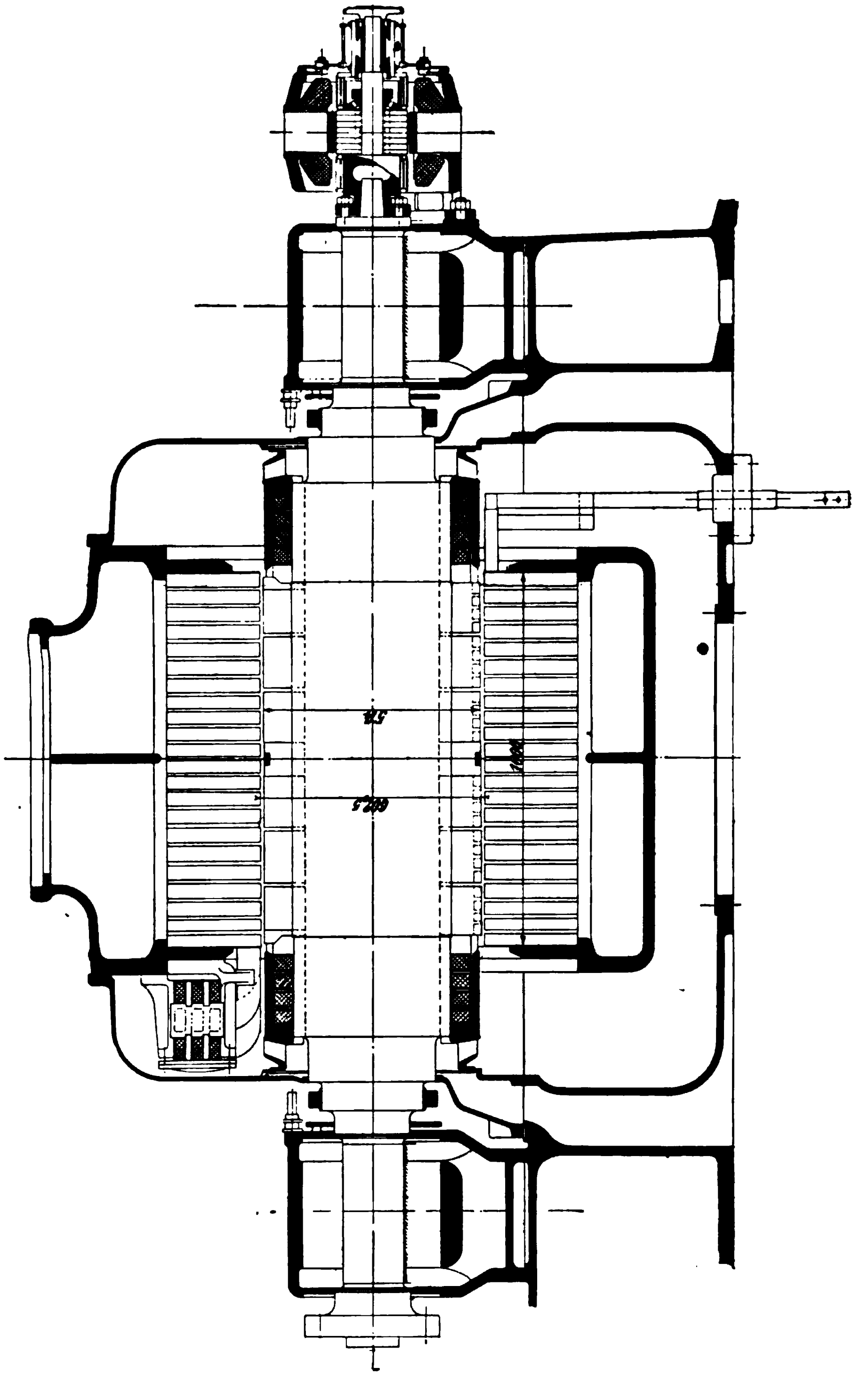


FIG. 376.—2000 K.W. 2-pole turbo-generator built by the Allgemeine Elektricitäts Gesellschaft. Speed 3000 R.P.M.

The deflection of the rotor can be worked out by the well-known graphical method. Where the rotor consists of steel punchings threaded on a shaft it is found in practice that the amount of stiffness afforded by these punchings, even when very firmly bolted together, is generally very small, and may be neglected in comparison with the stiffness of a strong shaft. The punchings, however, absorb a considerable amount of the energy of the whipping action, and enable a rotor which is not very badly out of balance to run through the critical speed without

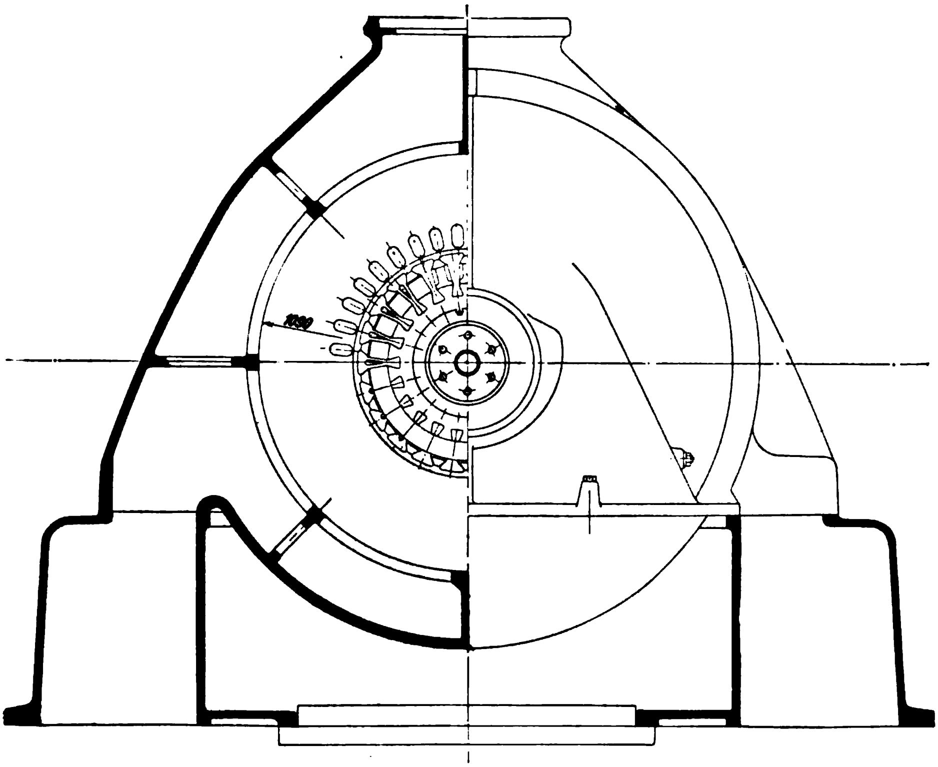


FIG. 377.

excessive vibration. Where the rotor is cut out of a solid steel forging the critical speed can be pre-determined with greater accuracy than where it consists of various parts pressed or shrunk on to the shaft.

We choose a diameter which by trial gives us the required output with an axial length which permits of the required stiffness of shaft. If the diameter is about one-half the axial length of the iron, the proportions are generally good mechanically, and economical electrically, for two-pole turbo-alternatives. The general proportions of the machine are the same as those shown in Figs. 376 and 377, which show a 50-cycle 1500 K.W. generator built by the A.E.G. for a speed of 3000 R.P.M. Figs. 354 to 357 (page 369) show the method of constructing the rotor of this machine.

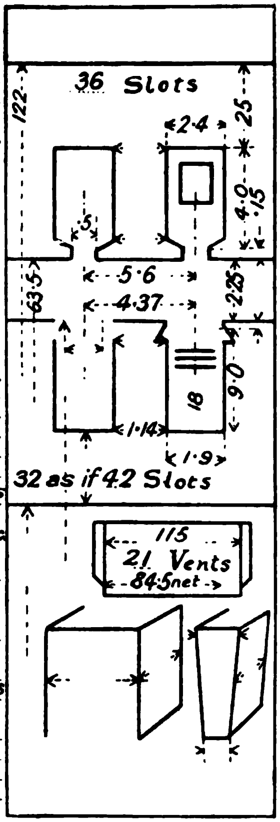
It will be found that for these high-speed two-pole turbo-generators with poor regulating qualities we can take an output coefficient between 500,000 and 600,000,

Date 2nd Feb. 19 13 Type Turbo A.C. GEN. ~~SYN MOTOR ROTARY~~ 2 Poles — Elec Spec. 6
K V A 2500: P F .8: Phase 3 ; Volts 550 ; Amps per ter 2620 , Cycles 50 ; R P M 3000: Rotor Amps
H.P. Amps p. cond. 1310 Amps p. br. arm. Temp rise 40°C Regulation Overload 25% 2 hrs

Customer ; Order No. , Quot No. , Perf Spec . Fly-wheel effect

Frame 122/115 Circum 192; Gap Area 22100 poss $A_g B$; poss $I_a Z_a$; $I_a Z_a$ / Circum. ; $\frac{D^2 L \times RPM}{K.V.A.}$ = 5.6 × 10⁵
Air 4 cu. m. per sec. $A_g B$ 1.55 × 10⁸ ; $I_a Z_a$ 47200 , Circum. 197 ;
K_e .392 , 550 Volts .392 . 50 × 18 · 1.55 , Arm. A.T p. pole... 10,300 Max. Fld A T 26,000

Armature. ~~Rev.~~ Stat.		
Core. Dia Outs.	122	
Dia Ins	63.5	
Gross Length	115	
Air Vents 21	.95	
Opening Min 2300 Mean	4900 sq. cm.	
Air Velocity	92 m. p. sec.	
Net Length 95 × .89	84.5	
Depth b. Slots	25	
Section 2120 Vol	6.45 × 10⁵	
Flux Density	11,700	
Loss .06 p. cu. cm. Total	39,000	
Buried Cu. 3300 Total	43,400	46,700
Gap Area 19000 Wts	12,300	
Vent Area 450,000 Wts	32,000	
Outs. Area 43,000 Wts	6 500	50,800
Teeth. No of Segs 6 Mn. Circ.	212	
No of Slots 36 × 2.4 =	86.5	
K_s	125.5	
Section Teeth	10,600	
Volume Teeth	44,000	
Flux Density	14,500	
Loss 1 p. cu cm Total	4,400	
Weight of Iron	5500 kilogrs	
Conductors. Star ~~or Mesh~~ Throw	1 to 14, 2 to 15, &c	
Cond. p Slot	½	
Total Conds 36/2	18	
Size of Cond 1.9 × 1.9	3.5 sq. cm.	
Amp. p. sq. cm	375	
Length in Slots 115		
Length outside 147 Sum	262	
Total Length	95 m	
Wt. of 1,000 3120 Total	300 kilogrs	
Res. p 1,000 .048 Total	.0047/4	.00115
Watts p.		
Surface p.		
Watts p. Sq.		



Field ~~Stat. or~~ Rotor.		
Dia Bore	59	
½ Total Air Gap	2.25	
Gap Co-eff K_g	1.08	
Pole Pitch Pole Arc		
K_f	.625	
Flux per Pole 47.5 × 10⁶		
Leakage n L 3 × 10⁶ f 1.6 × 10⁶	53.5 × 10⁶	
Area 3940 Flux density	13,500	
Unbalanced Pull		
No of Seg. 1 Mn Circ	128.4	157
No of Slots 42 × 1.9 =	80	80
Vents 21 95	48.4	77
K_s 3 48 Section	4600	7300
Weight of Iron		

	Shunt.	~~Series.~~	~~Comp.~~
		F.L.	25%
A.T. p Pole n. Load	14,000		
A T. p Pole f Load		25,500	29,000
Surface	66,000		
Surface p Watt	3.3		
I² R	4000	15,100	
I. R.	40.5	85	100
Amps.	97	177	200
No. of Turns	144		
Mean 1 Turn	250 cm	130 cm = 380	
Total Length	720 m + 372 m.		
Resistance	.395 cold	.475 hot	
Res per 1 000	.38 and .3		
Size of Cond	.305 × 1.5 and	.305 × 1.9	
Conds per Slot	18		
Total			
Length	1092		
Wt per 1,000	445		
Total Wt	486 kilogrs		
Watts per Sq			
Star or Mesh			
Paths in parallel			

Magnetization Curve.	Section.	Length	500 Volts. B.	A.T. p. cm	A.T.	550 Volts B.	A.T. p cm	A T	575 Volts B.	A.T. p cm	A T
Core	2120	63	10200	3.5	220	11,200	5	320	11,700	6	380
Stator Teeth	10,600	4	13200	9	36	14,600	16	64	15,200	21	84
Rotor Teeth	1820	9			40	13,700	9	100	14,500	16	144
Gap	22,100	2.25			12,300	7000		13500	7,350		14,200
Pole Body					100			112			192
Yoke					12696			14096			15,000
					200			800			1500
					12896			14896			16500

Commutator.	
Dia Speed	
Bars	
Volts p. Bar	
Brs. p. Arm	
Size of Brs	
Amps p sq	
Brush Loss	
Watts p Sq	

EFFICIENCY	1¼ load.	Full.	¾	½	¼
Friction and W.	46	46	46	46	46
Iron Loss	47	47	47	47	47
Field Loss	20	15	11	8	4.5
Arm. &c. I²R	13	8	5	2	.5
Brush Loss					
	126	116	109	103	99
Output	2500	2000	1500	1000	500
Input	2626	2116	1609	1103	599
Efficiency	95.2	94.5	93.2	90.5	85

Mag Cur Loss Cur
Perm Stat. Slot
,, Rot Slot × =
,, Zig-zag
2 × ×
1.77 × =
End × × =
Amps. Tot
τ = : X_a =
S_1/S_2 ; r_a = +

Imp √ + =
Sh cir Cur
Starting Torque
Max Torque
Max. H.P.
Slip
Power Factor

the dimensions being in centimetres. An internal diameter of stator of 63·5 cms. gives a rotor diameter of 59 cms., and an axial length of 115 cms. These proportions are suitable, the output coefficient being 560,000.

A section of the iron of the stator is given in Fig. 240. Particulars of the iron loss and windage loss, as determined by experiment, are given on page 244, and the distribution of temperature with various amounts of cooling air are given in Figs. 241 to 247. The rating of the machine upon which these tests were carried out was 1870 K.W., but that rating was fixed in order to meet a certain regulation guarantee. The frame can be rated at 2000 K.W. for a poorer regulation with the iron worked at exactly the same state of saturation.

Particulars of the windings on stator and rotor are given in the calculation sheet on page 408, and method of calculating the various quantities will be easily understood from the description of the method given on pages 316 and 332. It is therefore unnecessary here to go through the sheet in detail, but the reader will be interested in comparing the results arrived at by this method of calculation with the actual results experimentally obtained. The rotor winding consists of concentric coils of the type shown in Fig. 359, but the parts of the coils lying outside the slots have a section of copper $0·305 \times 1·9$ cms., while inside the slots the section is $0·305 \times 1·5$ cms. This helps doubly in keeping down the temperature of the end connections. It gives a lower current density, and it gives a great section of copper for the conduction of the heat to the straight parts of the coils, where most of the cooling surface is (see page 225).

25-CYCLE TURBO-GENERATORS.

A two-pole machine to generate at 25 cycles cannot have a speed * higher than 1500 R.P.M. This is a drawback from the turbine builder's point of view, and is one of the reasons why 25-cycle turbo-generators are not often built in small sizes. Even for large sizes, where 1500 R.P.M. is quite economical for the steam turbine, the two-pole generator is much more costly than a four-pole generator of the same output. On account of the lower speed the diameter can be increased, so that outputs up to 25,000 K.W. or higher become possible. The very bulky end connections on these large two-pole machines make a very undesirable feature. The general proportions of a 25-cycle turbo-generator for 1500 R.P.M. will be seen from Fig. 378, which shows a 2500 K.V.A. 25-cycle turbo-generator built by the Oerlikon Company.

* Certain methods of construction have been suggested for enabling 25-cycle generators to be run at speeds higher than 1500 R.P.M. In some of these the field of the rotor is a polyphase field, which rotates backwards relatively to the rotor iron. In another ingenious suggestion the poles on the rotor are distributed like the thread of a screw around the rotor surface, and the speed of movement of the pole relatively to the stator conductors can be made as slow as desired by making the pitch of the screw very small. These methods have not come into general use.

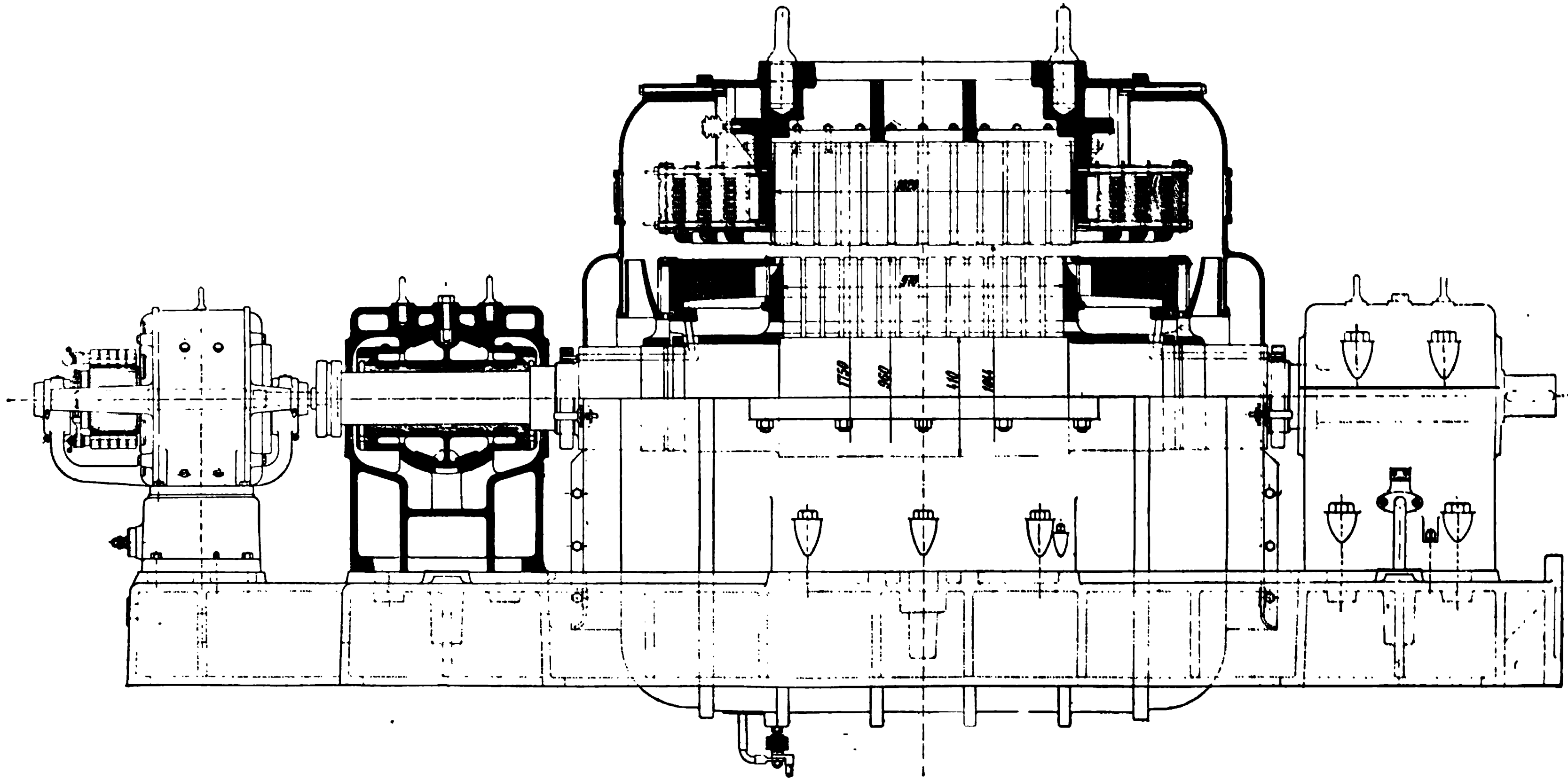


FIG. 378.—1500 K.V.A. three-phase 6000-volt turbo-generator, 25 cycles, 1500 R.P.M. (Oerlikon Company.)

SINGLE-PHASE GENERATORS.

Single-phase turbo-generators are sometimes now required for central stations which adopted a single-phase system in the early days of electric supply, and which have not yet changed over to polyphase. A certain number of low-frequency, single-phase generators are manufactured for single-phase traction, though where possible it is more economical to build a polyphase generator, and arrange for the different phases to be supplied to different parts of the system. Single-phase windings have been considered in Chapter VI. The most common practice is to take an ordinary three-phase armature and put the winding in two-thirds of the slots. Sometimes, for convenience in getting the right number of conductors, one will use rather more or rather less than two-thirds of the slots. It is not well to use much more than the two-thirds, or we shall get some coils which enclose only a small fraction of the total flux, and thus employ a large weight of copper for the output. The mechanical arrangement of the end connectors of the armature is, of course, much simpler than on three-phase machines, and the overhang of the coils is reduced.

The **armature reaction** of these machines is pulsating in character, and gives rise to pulsations in the field-flux, which may cause serious heating of the field-magnet, if proper precautions are not taken to prevent it. The most common plan is to provide the field-magnet with a **damper** or **amortisseur**, the eddy currents in which oppose any change in the value of the flux. This damper can be conveniently made on cylindrical rotors, by using copper wedges in the tops of the slots, and connecting electrically and mechanically with conducting end rings, so as to form a squirrel-cage winding. In calculating the cross-section of copper to be used in this squirrel-cage, we must remember that a single-phase armature reaction may be regarded as due to the sum of two vectors rotating in opposite directions. Each of these vectors represents a number of ampere-wires equal to one-half of the total ampere-wires on the armature. The vector, which rotates in the same direction and at the same speed as the field-magnet, does not produce any pulsation, but only a steady distortion, just as a polyphase reaction (see page 278). The vector which revolves in the opposite direction produces an eddy current in the squirrel-cage winding of double frequency. The number of ampere-wires in the phase-band of eddy current is equal to one-half the phase band of current on the armature. We must therefore provide a cross-section of copper in the damper sufficient to carry one-half of the ampere-wires on the armature. At the same time we must remember that the high frequency of the damper current will produce eddy current losses in any solid metal parts enclosed in the magnetic circuit of the damper winding, and accordingly arrange all surrounding parts so that they are either of such good conductivity that the eddy current can flow without causing excessive loss or of such high resistance that no appreciable loss can occur in them.

If we take an ordinary star-connected, three-phase generator, having terminals A, B and C, and load it as a single-phase machine by putting a load across the terminals A and B, we will find that one kilowatt of single-phase load will produce

about 1·35 times as much reaction on the field-magnet as one kilowatt of three-phase load. As the output of the generator is usually limited by the output of the field-magnet, we may say that as a single-phase generator a frame will only carry about 0·74 of the load it would carry as a polyphase generator.

The reader will find useful data relating to single-phase generators in the articles * quoted below.

* "Modern Development in Single-Phase Generators," W. L. Waters, *Amer. I.E.E.*, Proc. 27, p. 579, 1908; "Comparative Capacities of Alternators for Polyphase and Single-Phase Currents," *Elec. Journ.*, 8, p. 672, 1911.

CHAPTER XVI.

INDUCTION MOTORS.

We shall assume that the reader is familiar with the general theory of the induction motor and the use of the Heyland circle diagram. Different writers give the circle diagram of the induction motor in different forms. It is therefore convenient to

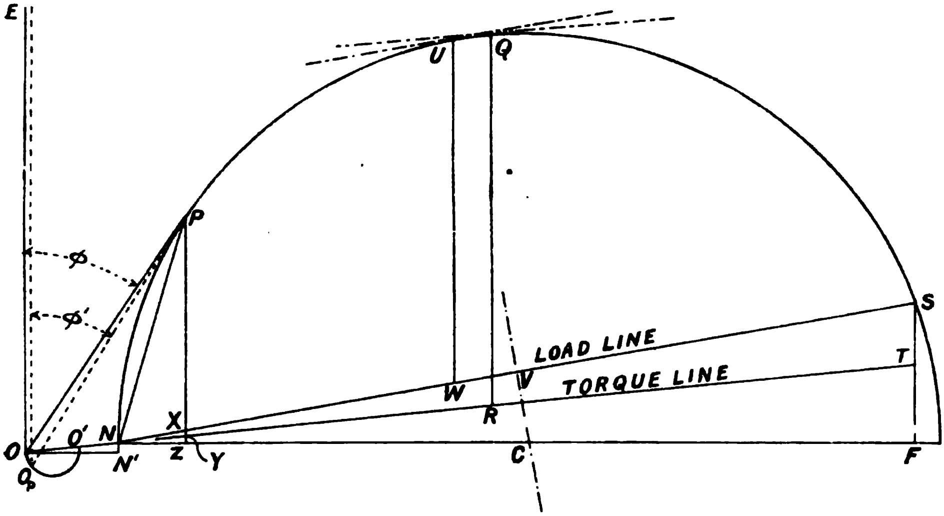


FIG. 400.—Circle diagram of induction motor.

reproduce here a form* which is found to be very convenient in workshop use, and to give results which check sufficiently well with those obtained in practice.

* See Karapetoff's *Experimental Electrical Engineering*, vol. 2, page 166 (Chapman & Hall, London; John Wiley & Sons, New York); Cramp and Smith, *Vector Diagrams* (Longmans); "Graphical Treatment of the Rotating Field," R. E. Hellmund, *Amer. I.E.E.*, Proc. 27, p. 927, 1908; "Circle Diagram for the Induction Motor," J. Yernaux, *Soc. Belge. Élect.*, Bull. 27, p. 246, 1910; "Circle Diagram of the Induction Motor," W. Petersen, *Elektrotech. Zeitschr.*, 31, p. 328, 1910; "Polyphase Induction Motor, Circle Diagram of," K. Krug, *Elek. u. Maschinenbau*, 28, p. 1047, 1910; "Circle Diagram for 3-phase Induction Machines," T. F. Wall, *I.E.E. Journ.*, 48, p. 499, 1912; "Vector Diagram of the Induction Motor on an Experimental Basis," L. Dreyfus, *Archiv. f. Elektrot.*, 1, p. 124, 1912; "Simple Graphical Construction for Determining the Efficiency of a Polyphase Asynchronous Motor from the Current (Circle) Diagram," J. Nicolson, *Journ. I.E.E.*, 49, p. 297, 1912.

When the no-load data of the motor are available we can construct the Heyland diagram shown in Fig. 400 as follows:

Choose a current scale (say, 1 mm. = 10 amperes). Let the lines on the diagram represent amperes per phase to this scale.

ON	represents the	no-load current per phase.
N'N	„	wattful current per phase supplying the no-load losses.
ON'	„	true magnetizing current per phase.
O'S	„	current per phase on short circuit.
SF	„	wattful current per phase supplying the losses on short circuit.
TF	„	wattful current per phase supplying the losses in stator on short circuit.
ST	„	wattful current per phase supplying the losses in rotor on short circuit.
OP	„	stator current on normal load.
NP	„	rotor current on normal load $\times \frac{S_2}{S_1}$.
PX	„	wattful current supplying useful work.
PY	„	wattful current supplying power to rotor.
PZ	„	wattful current supplying power to stator and rotor.
XY	„	wattful current supplying rotor losses.
YZ	„	wattful current supplying stator losses.
ϕ	„	angle of lag neglecting stator losses.
ϕ'	„	angle of lag after approximate correction for stator losses.
UW	„	wattful current per phase supplying the maximum power of the motor.
QR	„	wattful current per phase supplying the maximum torque to the motor.

The slip is given by the ratio $XY : PY$.

To convert any *vertical* line into watts, multiply the number of amperes per phase by the line voltage and by 1·73.

To convert a *vertical* line into torque in lbs. at a foot radius. Multiply the number of amperes (obtained by scaling it off) by

$$\frac{\text{Line volts} \times 1{\cdot}73 \times 33{,}000}{746 \times \text{syn. } R_{pm} \times 2\pi}.$$

The usual method of procedure is to set off the no-load current *ON* to scale. To find its position with respect to *OE* we may either set off *EON*, the angle of lag at no load, or we may set off *N'N*, the wattful current at no load (see page 420). Then set off *O'S*, the short-circuit current. The point *O'* is chosen, so that $\frac{OO'}{O'N} = \frac{\text{stator impedance}}{\text{rotor impedance}}$. Usually it is sufficient to put *O'* half-way between *O* and *N*. *SF* can be calculated by dividing the watts lost on short circuit by the line volts and by 1·73. Thus we obtain the **load line** *NS*. To get the centre of the semi-circle, we bisect *NS* in *V* and draw *VC* at right angles. Where this cuts the horizontal line *NF* is the centre *C*. We can then draw the semi-circle through

N and S. This gives us the locus of the point P for normal loads. For loads heavy enough to cause saturation of iron along the leakage paths, the point P moves on a rather wider curve, as shown by the dotted curve in Fig. 415. For normal loads, and even up to the maximum output, the semi-circle NPU gives the locus of P with sufficient accuracy for practical purposes. It is usual to take the angle ϕ as the angle of lag of the stator current behind the voltage, but this is only right when the stator-resistance drop and the change in the value of ON can be neglected. An approximate method of allowing for the effect on the power factor of the stator resistance and the change in the value of ON is to shift the position of the origin O and make it travel around the little semi-circle OO_PO', as P travels around its semi-circle. Thus, if we take the origin at O_P instead of at O, we get the angle ϕ' as the angle of lag, and this is rather smaller than ϕ. In actual practice, however, the power factor depends somewhat on the wave-form, and as this is generally unknown when a motor is being sold, it is safer to base guarantees of power factor on the angle ϕ, and keep in hand any advantage that may afterwards be derived from the fact that ϕ' is smaller.

Any vertical line drawn from P and cutting NS, the load line, gives by its intercept (such as PX) the wattful current per phase, supplying the output of the rotor. It is thus proportional to the output of the motor, and to get the output in watts it is only necessary to multiply the number of amperes represented by the vertical intercept by the voltage and by 1·73.

The maximum output is obtained from the vertical line UW, drawn from U. where the tangent parallel to NS touches the semi-circle.

To get the torque line we must divide FS into two parts, such that

$$\frac{ST}{TF}=\frac{r_{2.1}\times NS^2}{r_1\times O'S^2},$$

when $r_{2.1}$ and r_1 are obtained as shown on page 428. Any vertical line drawn from P cutting the torque line NT gives, by its intercept (such as PY), the wattful current per phase supplying the input to the rotor. As the torque multiplied by the synchronous speed gives us the input into the rotor, we can obtain the torque in lbs. at a foot radius by multiplying by the constant given on page 414. The maximum torque is obtained by drawing a vertical QR from the point Q, where a tangent parallel to NT touches the semi-circle.

Circle diagrams drawn to scale are worked out on pages 457 and 474.

In working out a circle diagram we may be able to start with data given by experiments on the motor in question, or we may have to start with the particulars of the design, and deduce the leakage flux and magnetizing current, etc., from the dimensions. When the no-load and short-circuit data are given, the working out of the power factor and other particulars of performance from the circle diagram is a comparatively simple matter, and is indicated in the method followed in the examples given below.

When the no-load data are not given and have to be deduced from the dimensions, the calculations are somewhat more lengthy, but they can be shortened by the judicious use of formulae founded on practical results. The principal quantities to be determined are the magnetizing current and the leakage flux.

DETERMINATION OF THE MAGNETIZING CURRENT OF AN INDUCTION MOTOR.

Following the general method adopted throughout this book, we concern ourselves first with the maximum flux-density in the gap. This will usually be found to be much lower in induction motors than in A.C. generators, because of the importance of keeping down the magnetizing current. If too high a flux-density in the gap were chosen, not only would the magnetomotive force on the gap be great, but the teeth, being necessarily of wide section to carry the heavy flux, would leave little room for copper, and thus the number of turns per pole would be few and the exciting amperes in consequence great. In an A.C. **generator** we choose a low number of turns per pole on the armature to improve the regulation. In an **induction motor** we choose a high number of turns per pole, in order to keep down the magnetizing current. The increasing of the ampere-turns on the armature of an induction motor must not, however, be carried too far, because the more we reduce the flux per pole the greater we make the ratio between the leakage flux and the working flux, and the smaller we make the diameter of the main circle of our diagram.

The flux-density in the gap. The maximum flux-density found in induction motors usually lies between 5000 and 7000 lines per sq. cm. $B=6000$ is a common figure. In low-voltage motors of large size it will be more, and in high-voltage motors it will be less. In motors designed to be used in conjunction with a phase advancer, the flux in the air-gap may be carried to as high a figure as 9500. The reader will see from design sheets on pages 448 and 471 the general considerations which fix the density in the gap. Often in high-voltage motors the amount of room taken up by the stator coils and insulation so reduces the section of the teeth as to necessitate a rather low value for the flux-density in the gap.

Length of air-gap. The length of air-gap in induction motors is made as short as is compatible with securing a good mechanical clearance. In very small motors, particularly if the surfaces of the rotor and stator are ground perfectly true, exceedingly small clearances, even down to 0·04 cm.. may be employed. On large machines the length of air-gap is generally increased. The curve in Fig. 401 shows the relation between the length of the air-gap and the diameter of rotor according to good practice, where precautions are taken to avoid distortion of the frame. If the air-gap is made too short, the unbalanced magnetic pull due to extremely small accidental displacements may be excessive, and by causing a further displacement may bring the rotor in contact with the stator. Where it is desired to keep the power factor of a large induction motor with numerous poles as high as possible, and therefore the magnetizing current as low as possible, the designer is tempted to reduce the air-gap to the smallest permissible figure. For this purpose he arranges the stiffness of the frame and shaft so that they will withstand a heavy unbalanced magnetic pull without undue distortion. One method of greatly reducing the unbalanced magnetic pull is to connect the two halves of the stator winding in parallel, each half of the winding occupying coils on opposite sides of the diameter, in the manner shown in Fig. 409. In this case the division of the two halves of each phase should take place about diameters placed at angles of

60° to one another. It is not then possible for the magnetic flux on one side of one of these diameters to be much greater than on the other, because the greater flux would produce a greater back electromotive force and keep down the magnetizing current on the side which, by reason of its short air-gap, might otherwise tend to have an excessive magnetic flux.

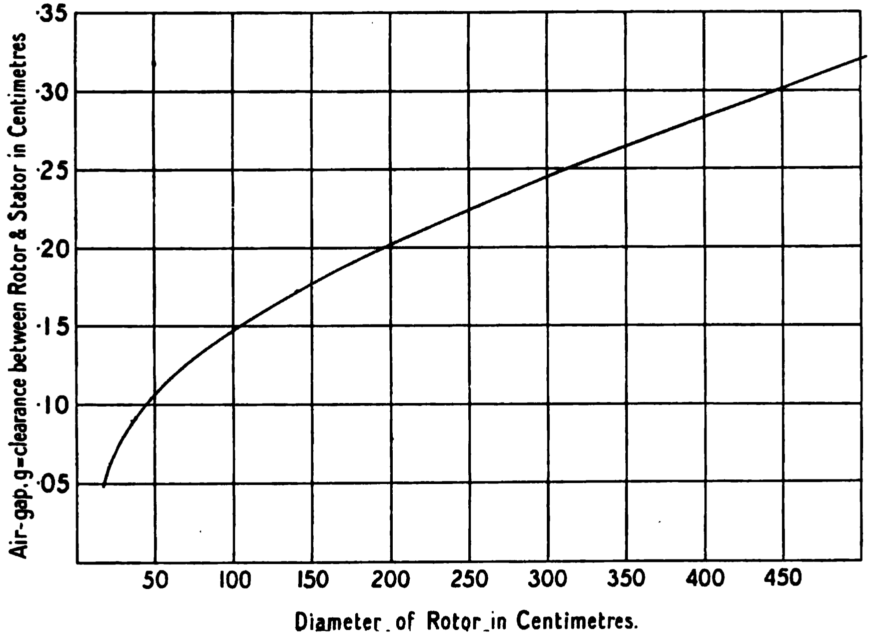


FIG. 401.—The length of air-gap in induction motors.

The calculation of ampere-turns on the gap. The general method of calculating the ampere-turns on the air-gap, given on page 65, is applicable to induction motors. In general, the air-gap coefficient K_g of an induction motor is much higher than for an A.C. generator, on account of the greater ratio of width of slot to length of gap.

EXAMPLE 48. Take the dimensions of slot, gap and ventilating duct from the calculation form on page 448. We have

$$\frac{s}{g}=\frac{0{\cdot}3}{0{\cdot}2}=1{\cdot}5 \quad\text{and}\quad \frac{s}{p}=\frac{0{\cdot}3}{2{\cdot}6}=0{\cdot}115.$$

From Fig. 37, page 67, we have the contraction ratio for stator slot 1·03. Similarly for rotor slots it is 1·04. Next, for the ventilating ducts

$$\frac{s}{g}=\frac{0{\cdot}8}{0{\cdot}2}=4{\cdot}0 \quad\text{and}\quad \frac{s}{p_s}=\frac{7\times 0{\cdot}8}{47}=0{\cdot}12.$$

From Fig. 36 the contraction ratio for the ventilating ducts on the stator is 1·05. It is the same for the rotor ducts. Taking the product of all these, we have

$$1{\cdot}03\times 1{\cdot}04\times 1{\cdot}05\times 1{\cdot}05=1{\cdot}18=K_g$$

for the gap of the induction motor.

The ampere-turns on the gap are equal to

$$5700\times 0{\cdot}2\times 1{\cdot}18\times 0{\cdot}796=1070.$$

The calculation of ampere-turns on the teeth. This is carried out in the same way as described on page 73. In many cases the ratio K_s (see page 71) in induction motors is fairly high on account of the small section of the teeth; and where high saturations are used it is desirable to have recourse to Fig. 47 in order to find the actual ampere-turns on the teeth, because the apparent flux-density differs appreciably from the actual flux-density.

The permissible flux-density in the teeth is limited in 50-cycle motors by the permissible iron loss per cu. cm. of tooth. It may be between 16,500 and 17,500, depending upon the cooling conditions. At low frequencies the density is limited by the number of ampere-turns that may be applied to the teeth. Too high a density will require too great a magnetizing current and spoil the power factor of the motor. Densities of 18,500 to 20,000 lines per sq. cm. are not uncommon

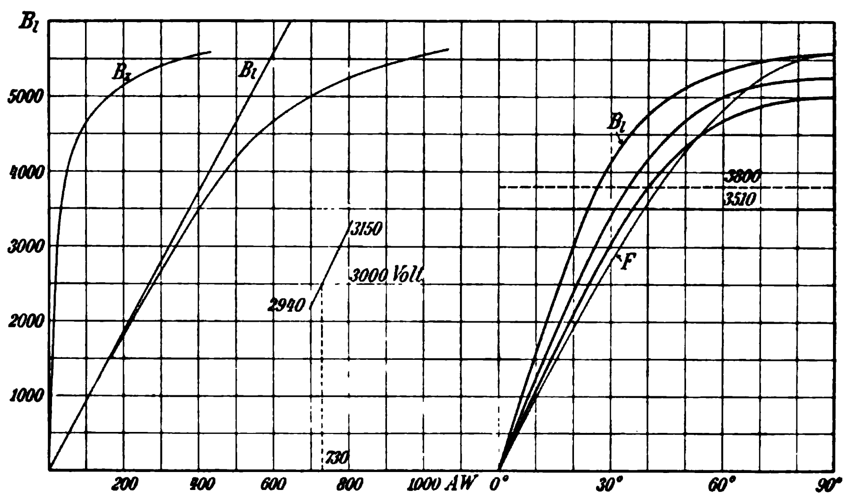


FIG. 402.—Method of finding the average field-form of an induction motor.

in 25-cycle motors. Where a phase advancer is used in conjunction with the motor, and the frequency is not so high as to make the iron loss excessive, densities as high as 21,000 lines per sq. cm. are permissible.

Strictly speaking, the maximum density in the teeth can only be ascertained at any particular voltage on the stator winding by plotting the field-form of the motor, and from it the values of the E.M.F., as we have shown on page 32. In actual practice, the ampere-turns required for the teeth are known from experiments upon the particular stampings in question, for every state of saturation of the frame, so that no elaborate calculation is necessary. In cases where the ampere-turns are not already known from trial, and where the saturation is not very excessive, say, not higher than 17,000 lines per sq. cm., we may assume that the maximum density in the teeth is what it would be with a sine distribution of flux, and make our calculations accordingly without introducing very great error.

Where the saturation is very high it is possible to ascertain the approximate shape of the field-form by the method given on page 21, aided by a curve giving

the ampere-turns on gap and teeth for each value of the flux-density on the gap. The method of plotting such a curve is given on page 78.

In Fig. 402 the curve B_z on the left gives the ampere-turns (A.W.) on the teeth of a 3000-volt induction motor for different values of B in the gap. The straight line B_l gives the ampere-turns in the gap. The combination of these gives the ampere-turns on teeth and gap. Taking now a certain magnetizing current passing through a distributed winding, giving, say, a total of 700 ampere-turns per pole, we can, in the manner shown in connection with Fig. 15, aided by our combined tooth and gap saturation curve, plot and approximate field-form shown by the lower full line curve on the right. From this field-form we can calculate the generated E.M.F. (see page 32). Say that this is 2940 volts. Now plot another field-form for a higher total number of ampere-turns, say 800, and calculate the E.M.F. generated by that field-form. Say that this is 3150. Then the ampere-turns required for the normal voltage of the machine will, for small variations, be almost in proportion, so that for 3000 volts they will be about 730

The amount of space taken up by the rotor copper and insulation is usually much smaller than that required for the stator copper and insulation. It thus comes about that there is usually a more liberal allowance of iron in the rotor teeth, so that the number of ampere-turns on these teeth is often small.

Ampere-turns on the core. In low-frequency machines which have a fairly great pole pitch, and in which the flux-density in the iron is carried up to a fairly high point, some allowance must be made for the ampere-turns required on the cores behind the slots. The amount is small in comparison with the other ampere-turns, and therefore no time need be wasted in making an accurate calculation. It is sufficient to assume that the ampere-turns on the core are equal to the ampere-turns that would be required on a core length of one-third of the pole pitch, in which the flux-density is equal to the maximum density found in the core of the machine in question.

EXAMPLE 49. In the motor illustrated in Fig. 407, the pole pitch is 31·2 cms. The maximum flux-density in the core is 8450 lines per sq. cm. Find approximately the ampere-turns per pole required for the core. Take the effective length one-third of the pole pitch, say 10·5 cms. For a flux-density of 8450, we require about 3 ampere-turns per cm. $10{\cdot}5 \times 3 = 32$ ampere-turns per pole on the core. These are so low that in practice they could be neglected, because they are smaller than the errors coming into other parts of the calculation.

Suppose now that the motor was working at 25 cycles with a flux-density in the core of 16,000. The ampere-turns on the core would then be $8 \times 30 = 240$.

In the calculation sheet given on page 448 will be found a table of the ampere-turns required on various parts of the magnetic circuit when the motor is operating at 3000 volts.

Magnetizing current. After we have found the total number of ampere-turns required to produce the flux-density in the centre of the pole, it remains to calculate the number of virtual amperes of magnetizing current to be supplied to each terminal of the motor. The commonest case with which we shall have to deal will be the case of a three-phase star-connected stator having a full pitch winding arranged as in Fig. 110, with two, three, four or more slots per phase per pole. For this type of winding it has been shown on page 280 that the average value of the

ampere-turns per pole exerted by the armature is approximately equal to 0·437 $I_m Z_a$, so that $I_m = \frac{\text{A.T.}}{0·437 Z_a}$, where I_m stands for the virtual value of the wattless magnetizing current. To arrive at the core loss current I_c, we divide the calculated (or measured) iron loss by the voltage and by 1·73. The total magnetizing current I_{mt} will then be $\sqrt{I_m^2 + I_c^2}$. It can be conveniently obtained by a graphic construction. Where the winding is not of the common kind presupposed here, the safest plan is to lay out a diagram of the slots with the windings belonging to the various phases indicated in their respective positions. Then, assuming that one phase is at its maximum and the other two phases at one-half their maximum, plot the magnetomotive force wave produced thereby, as shown in Fig. 15. From this the virtual amperes per phase required to produce a certain maximum number of ampere-turns on the centre of the pole is at once apparent. Then take two phases at 0·866 of their maximum value, while the other phase is at zero, and make a similar plot from which the virtual amperes per phase for the same ampere-turns on the pole can be ascertained. A mean of the values obtained in the two cases will give the magnetizing current with sufficient nearness for practical purposes.

The no-load current. If the no-load losses (iron loss and friction losses) and the magnetizing current are known, the no-load current is obtained as follows. Let W_n be the no-load losses in watts and E_t the volts at the terminals; then

$$\frac{W_n}{1·73 \times E_t} = \text{current per phase supplying the no-load losses} = I_{nl}.$$

Set off I_m horizontally, ON', and I_{nl} vertically, $N'N$; then the hypotenuse ON, is the no-load current per phase (see Fig. 400).

DETERMINATION OF THE SHORT-CIRCUIT CURRENT BY CALCULATION FROM THE DESIGN.

If the rotor winding of an induction motor be short circuited and voltage applied to the stator, the windings of the stator and rotor form a compound impedance the value of which depends upon (1) the amount of magnetic flux leaking between the primary and secondary members; (2) the ohmic resistance of the two windings.

The most accurate method of predetermining the short-circuit current of an induction motor is from tests on motors built on the same or similar frames. This is the method generally adopted in practice. The full calculation of the short-circuit current from all the factors which influence it would be a very lengthy matter, and at best would not be very accurate, because there are always some factors (such, for instance, as the amount of saturation of the iron) which depend upon accidents in the construction of individual motors. Any method of calculation from the dimensions of the motor, if it is to be of practical service, must be fairly short. In a short method we must be content to take into account only the most important factors, and aim not so much at an accurate determination of the short-circuit current in any particular motor, as at an appreciation of the way

in which different factors affect the result. The method given here enables the designer to judge between alternative designs for the same motor, and to tell roughly which will give the larger short-circuit current. At the same time, the method is probably as accurate as any other method, when we take into account the way in which indeterminate factors always influence the result.

On this subject the reader is referred to the articles* mentioned in the footnote.

The value of the short-circuit current depends mainly upon the ratio between the total working flux ϕ_P and the leakage flux ϕ_l for a stator current of 1 ampere. If the resistances of the windings could be neglected (and in practice they affect the result to only a very small extent), we could say that the leakage flux set up on short circuit is great enough to generate in the stator winding as much back E.M.F. as the total flux does at no load. If we neglect the difference in the breadth coefficients which affect the E.M.F. generated by the fluxes, we can say that the leakage flux on short circuit is equal to the working flux at no load. If we further assume that the leakage flux is proportional to the stator current, we may write ϕ_l for the leakage flux per pole for one ampere in the stator, and $I_a\phi_l$ for the leakage flux per pole for any stator current I_a.

Then, if $I_n\phi_l$ is the leakage flux at no load, and ϕ_p normal flux per pole,

$$\frac{I_n\phi_l}{\phi_P}=\frac{I_n}{I_{sc}}=\tau.$$

This ratio between the no-load current and the short-circuit current, or between the leakage flux at no load and the total flux here denoted by τ is a very important ratio, and forms the basis of the construction of the Heyland diagram. It depends upon the ratio of the magnetic reluctance of the main magnetic circuit to the magnetic reluctance of the leakage paths. Any change in the design of the motor which increases the reluctance of the leakage paths or decreases the reluctance of the main magnetic path, will decrease the value of τ and increase the ratio of the short-circuit current to the magnetizing current.

The behaviour of an induction motor when short circuited, with the rotor locked so that it cannot revolve, is similar to the behaviour of a short-circuited transformer having considerable magnetic leakage between the primary and secondary coils. The main difficulty in calculating the impedance from particulars

* "Leakage Problems of Induction Motors," R. Goldschmidt, *Electrician*, 69, pp. 236, 352, 430, 507, 624, 1907-8; "Leakage Factor of Induction Motors," R. E. Hellmund, *Elec. World*, v. 50, p. 1004, 1907; *Elec. World*, 51, p. 179, 1908; *Elect. Rev.*, N.Y., 52, p. 172, 1908; *Elektrot. Zeitschr.*, 30, p. 25, 1909; *Elektrotech. Zeitschr.*, 31, pp. 1111 and 1140, 1910; *I.E.E. Journ.*, 45, p. 239, 1910; "Predetermination of Short-circuit Current of 3-phase Induction Motors," W. Oelschläger, *Elektrotech. Zeitschr.*, 28, p. 1230, 1908; "Determination of the Circle Coefficient of the Induction Motor," H. M. Hobart, *Elec. Rev. and West. Electn.*, 55, p. 1073, 1909; "Calculation of Overhang Stray Flux in Induction Motors," U. Kloss, *Elek. u. Maschinenbau*, 28, p. 53, 1910; "Leakage of Induction Motors," W. Rogowski, *Elektrot. Zeitschr.*, 31, pp. 1292 and 1316, 1910; "Induction Motor Design Constants," A. M. Gray, *Elec. World*, 58, p. 1599, 1911; "Induction Motors, Reactance of," J. Rezelman, *Electrician*, 66, p. 857, 1911; "Doubly-Linked Dispersion of Asynchronous Motors," F. Niethammer & E. Siegel, *Elektrot. u. Maschinenbau*, 29, p. 635, 1911; "Experimental Determination of Leakage Factor of Transformers and Induction Motors," Benischke, *Elektr. Kraftbetr. u. Bahnen*, 10, p. 83, 1912; "Air-gap Leakage Fluxes in 2-phase Motors and in 3-phase Motors with 2-phase Rotors," Meyer-Wulfing, *Archiv. f. Elektrot.*, 1, p. 363, 1912; "Tests on Induction Motors designed with Deep Rotor Slots," L. D. Jones, *Gen. Elect. Rev.*, 16, p. 229, 1913.

of the design lies in the estimating of the amount of magnetic leakage. Most writers divide the leakage flux into four parts:

(1) The leakage across the stator slots.
(2) The leakage across the rotor slots.
(3) The zig-zag leakage.
(4) The leakage around the ends of the coils both on rotor and stator where they project from the iron.

In addition to these there is a certain amount of leakage which interlinks with both stator and rotor windings where the M.M.F. of one does not balance the M.M.F. of the other.*

Slot leakage. The calculation of the amount of effective leakage across the slots is most easily carried out by means of the formula

$$\lambda_d = \frac{1}{3}\frac{h_c}{b},$$

where h_c is the depth of the slot after a deduction has been made for the thickness of the insulation between the copper and the bottom of the slot, and b is the breadth of the slot. By λ_d we denote the lines across the slot per cm. of axial length of slot for unit magnetomotive force. To this must be added the leakage across the mouth of the slot. Whether the slot is open or semi-closed the permeance across the mouth of the slot can be found from Fig. 54 (p. 81). This figure is constructed so that a designer can tell at once from inspection the effect of changes in the shape of the lips upon the permeance. The shape of the lip is indicated by shading, as shown in the figure, and the shading may extend either to the line OA, as shown, or to the line DC, or to the line $0{\cdot}2B$. The position of the small face P may be varied, so that the fraction $\frac{\text{mouth of slot}}{\text{width of slot}}$ has any value between zero and 1. At whatever point we choose to draw P, it is only necessary to continue up the vertical line from P shown in the figure until it cuts one of the curves C', A' or B', corresponding to the depth of the lip, and we can at once read off the permeance λ_m per cm. of axial length of slot. For example, in Fig. 54, the lip is supposed to be of the shape indicated by the shading, the value of $\frac{\text{mouth of slot}}{\text{width of slot}}$ being 0·375. If we carry up the perpendicular from P to the curve A', we find that the permeance in C.G.S. lines per cm. length of iron is 0·98. Had the lip been of a deeper design, so as to extend up to the dotted line DC, we should have carried our perpendicular up to the dotted curve C', and the permeance would then be found to be 1·2. If the lip is of a special shape, or has the angle of one of its faces different from that shown in the figure, it is easy to sketch on our figure a lip having the same permeance and having face angles enabling Fig. 54 to be instantly applied.

EXAMPLE 50. Take the stator slot belonging to the 1500 H.P. motor shown in Fig. 408a. Here the value of h_c is 3·7 and $b=1{\cdot}5$.

$$\lambda_d = \frac{1}{3}\frac{3{\cdot}7}{1{\cdot}5} = 0{\cdot}8.$$

* "Leakage in Induction Motors," W. Rogowski & K. Simons, *Elektrot. Zeitschr.*, 30, pp. 219 and 254, 1909.

Now the ratio $\frac{m}{b}=\frac{0\cdot3}{1\cdot5}=0\cdot2$, and the shape of the lip is such as to be bounded by the line OA in Fig. 54. Therefore

$$\lambda_m=1\cdot13, \quad \lambda_d+\lambda_m=1\cdot93.$$

When calculating the leakage due to the rotor slot, it is convenient to multiply the sum of $\lambda_d+\lambda_m$ obtained in the way shown in the last example by the ratio $\frac{\text{No. of stator slots}}{\text{No. of rotor slots}}$. This enables the result to be added directly to the stator permeance, and the total leakage can be calculated from ampere wires in the stator slot.

EXAMPLE 51. Take the rotor slot belonging to the 1500 H.P. motor (Fig. 408). Here the value of $h_e=3\cdot6$ cms. and $b=0\cdot96$.

$$\lambda_d=\frac{1}{3}\,\frac{3\cdot6}{0\cdot96}=1\cdot25.$$

The ratio $\frac{m}{b}=\frac{0\cdot3}{0\cdot96}=0\cdot31$. Therefore $\lambda_m=1\cdot2$ and $\lambda_d+\lambda_m=2\cdot45$.

Now there are 288 slots in the stator and 360 in the rotor, so that the total permeance of stator and rotor slots is

$$1\cdot93+\frac{288}{360}\times2\cdot45=3\cdot89 \quad \text{(see p. 448).}$$

Zig-zag leakage. There has been a great deal of discussion of recent years upon the subject of zig-zag leakage. Some authors hold that the only cross flux of this kind which should be taken into account is the flux which passes from stator to rotor backwards and forwards, interlinking with some of the stator and some of the rotor conductors, due to the fact that back magnetomotive force of the rotor currents is not everywhere balanced by the magnetomotive force of the stator currents. This cross flux may be spoken of as the "doubly interlinked leakage." In the opinion of other authors, there is a cross flux which zig-zags backwards and forwards across the air-gap by reason of the fact that at certain positions of the rotor teeth with respect to the stator teeth the open slots of the stator are in a measure short-circuited by the tops of the rotor teeth, and the open slots of the rotor are in a measure short-circuited by the tops of the stator teeth. The amount of the short-circuiting is a function of the numbers of teeth on stator and rotor, of the widths of the tops of the teeth, and the length of the gap. This true zig-zag leakage would occur however well balanced the stator and rotor magnetomotive forces might be (provided always that the tops of the teeth were staggered for some part of the time of revolution, as indeed they must be).

We will give a simple rule for the rough estimation of the zig-zag leakage which works well enough in practice; though by reason of the fact that it does not take into account all the factors which affect the result, it cannot be regarded as strictly accurate. As we said before, if a method is not short, it is of no use in practical design. The rule here given sacrifices all the minor refinements in order that it can be applied in 30 seconds. If the reader requires a more exact method, he is referred to Dr. Goldschmidt's paper mentioned on page 421.

The reluctance of the path of the zig-zag leakage is in the main proportional to the length of the air-gap. The width of the path changes as the teeth change their relative positions; but the maximum width of the path is one-half the width

of the tops of the teeth where these are equal in rotor and stator, and where these are unequal it is a function of the widths of the tops of the teeth.

If we assume that the dimensions of the teeth and the mouths of the slots are such as one generally finds in practice, it is possible, roughly, to take into account the changing width of the leakage path by means of a coefficient K_z, and we may write :

$$\lambda_z = K_z \times \frac{\text{pitch of slot}}{2} \times \frac{1}{\text{length of gap} \times K_g},$$

where λ_z denotes the lines of zig-zag leakage per cm. axial length of slot, for unit magnetomotive force applied across the mouth of a stator slot. The values of K_z which may ordinarily be employed in practice are given in Fig. 403 as a function of the ratio $\frac{\text{No. of stator slots}}{\text{No. of rotor slots}}$.

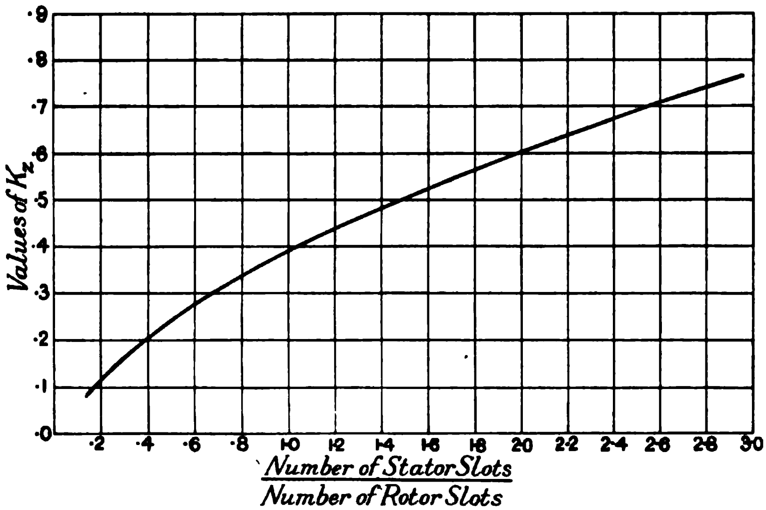


FIG. 403.—Values of K_z for estimating zig-zag leakage.

EXAMPLE 52. The slots in the stator and rotor of a 1500 H.P. motor are shown in Fig. 408. The air-gap $g=0{\cdot}2$ cm.; the contraction coefficient $K_g=1{\cdot}2$; the pitch of the stator slots $=2{\cdot}6$ cms. There are 288 slots in the stator (3 conductors per slot) and 360 slots in the rotor. Find the zig-zag leakage per pole for a core length of 47 cms. when the motor is on full load of 260 amperes per phase.

$$\frac{288}{360}=0{\cdot}8, \text{ and from Fig. 403 } K_z=0{\cdot}34,$$

$$\lambda_z=0{\cdot}34 \times \frac{2{\cdot}6}{2\times 0{\cdot}2\times 1{\cdot}2}=1{\cdot}84.$$

If we now add together the permeances due to the stator slot, the rotor slot and the zig-zag path per cm. of axial length, and multiply by twice the length of iron, we arrive at an approximate figure for the permeance of the path of magnetic leakage from one pole, so far as the first three parts of the leakage above referred to are concerned. Leaving out of account for the moment the leakage due to the end windings, we can get the leakage from the iron paths in C.G.S. lines per pole

by multiplying the total permeance above calculated by the maximum ampere-wires per slot and by 1·257.

EXAMPLE 53. In the 1500 H.P. motor shown in Fig. 407, we have

Permeance of leakage path across stator slot	=	1·93
,, ,, ,, rotor ,,	=	1·96
,, zig-zag leakage path	=	1·84
		5·73

Taking the particulars of the motor given on page 448:
Axial length of iron = 47 cms.
The permeance of the path = $5{\cdot}73 \times 47 \times 2 = 540$.
For a stator current of 1 ampere the leakage flux along the above paths is

$$540 \times 1 \times 1{\cdot}41 \times 3 \times 1{\cdot}257 = 2860 \text{ lines.}$$

The flux per pole leaking across the iron teeth for one ampere per phase in the stator we will denote by ϕ_i. It is the sum of the slot leakage and the zig-zag leakage when one ampere is passing in the stator. In the example given above $\phi_i = 2860$.

Leakage around the end windings. The only really accurate way of finding the value of the end leakage of an induction motor is by experiment on the winding in question. If we have two motors built on the same frame with the same type of winding, but one machine much longer than the other, we can, by measuring the short-circuit current on each machine, calculate with some accuracy what part of the leakage reactance in each machine is due to the end windings.

When once this has been ascertained it can be put on record and the figure used in similar cases.

In default of values found by experiment, it is desirable to have a simple method of finding roughly the amount of end leakage that may be expected on a given machine.

It will be seen that, while there are very many types and shapes of windings on induction motors, there are properties common to all the types found on commercial machines which make it possible to give approximate constants for the estimation of the end leakage. In the first place, where the coils are very deep they usually project a very long way out from the core; so that while the mean line of path encircling the coils is increased, the area of the path is increased in about the same proportion. Thus, for a given type of winding, say that illustrated in Fig. 201, the leakage per centimetre of perimeter will be about the same for the same ampere-turns per pole, independently of the size of the coils, always supposing that they are made to the same drawing, but to different scales. On the other hand, there is a great deal of difference between the amount of end leakage from coils of different types. It has been found by experiment (as is, indeed, obvious from inspection) that coils of the barrel type, as illustrated in Fig. 129, do not give half as much end leakage as coils of the concentric or chain type, as illustrated in Fig. 114. It will be sufficient for our purpose to introduce certain coefficients to take care of the characteristics of the different types of coils, and to include in our formula only those factors which have the greatest influence on the leakage per pole, assuming that the coil is of a standard type. As we are concerned in this formula with the leakage per pole, one of the main factors is the pole pitch.

Where the pitch is short and the coils project a long way from the iron, there is a great deal of sideways leakage that ought to be taken into account in the formula.

The amount of end leakage depends, not so much upon the number of ampere wires per slot, as upon the total number of ampere-turns per pole. The nearer the rotor and stator windings lie together, so as to neutralize each other in the creation of a magnetic field, the less will be the end leakage. Thus, if we have a barrel winding on both stator and rotor, the end leakage will be much less than if both rotor and stator windings are turned away from each other towards the iron. The further the windings project from the frame, the greater will be the leakage. The proximity of the iron parts, including the end plates and fenders covering the winding, greatly affects the end leakage. The whole matter is so complicated by accidental circumstances that it is useless to attempt any accurate calculation.

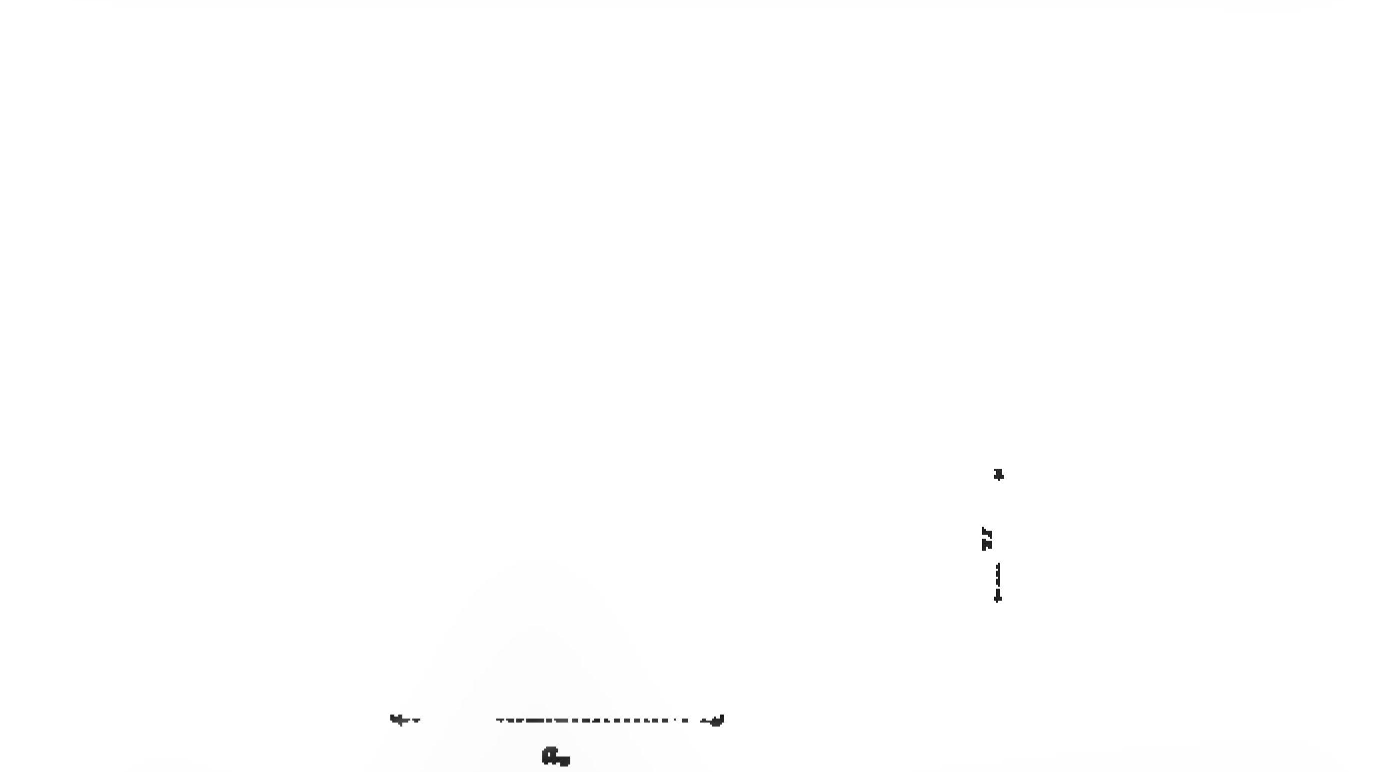


FIG. 404.—Showing dimensions in millimetres of the end winding (concentric type) of a 3000-volt induction motor. The average overhang of the coils, $a_v = 10$ cms.

In order to arrive at some rough idea to serve as a basis of calculation, we may divide the types of end windings into four separate classes, as shown in Table XVIII. To each combination of one type of stator winding with one type of rotor winding we may attach the coefficient K_L given in the table. These coefficients can then be used in conjunction with the following formula:

End leakage in C.G.S. lines per pole on both ends of machine,

$$I_a\phi_e = K_L \times (l_p + a_v) \times \text{virtual A.T. per pole},$$

where K_L has a value somewhere between 1·8 and 3·5, depending on the type of winding, as shown in the accompanying table, and

l_p = pitch of poles in cms.,

a_v = average overhang of coils in cms.

In Fig. 404 the average overhang of the coils is 100 mm., so that $a_v = 10$ cms.

The virtual A.T. per pole are taken in the following manner: Take the total number of conductors per phase per pole and multiply by the virtual amperes per conductor.

The end leakage on one pole really depends on how the end windings are arranged on that pole. There will be a difference, for instance, between the amount of flux encircling the hemitropic winding shown in Fig. 101 and the flux encircling the divided-coil winding shown in Fig. 102. If, however, we take the ampere-turns as directed above, and remember that it is the total leakage on two poles that must be taken into consideration, it will be found that the above method gives values which are near enough for practical calculations. The hemitropic winding usually has a larger a_v than the divided coil winding, and in that respect gives rather greater values for end leakage.

TABLE XVIII. VALUES OF K_L FOR END LEAKAGE OF THREE-PHASE MOTORS WITH NORMAL FULL-PITCH WINDINGS.

Type of Rotor.	Type of Stator Winding.		
	Barrel (Fig. 129).	Mush (Fig. 138).	Concentric (Fig. 114).
Squirrel cage (Fig. 413)	1·8	2·6	2·8
Barrel (Fig. 129)	1·4	2·4	2·45
Mush (Fig. 138)	2·2	3·1	3·2
Concentric (Fig. 114)	2·45	3·2	3·5

EXAMPLE 54. In the 1500 H.P. motor, particulars of which are given on page 448, the pitch of the poles l_p is 31 cms. and the average overhang a_v of the coils is 12·5 cms. There are 4 slots per phase per pole, and 3 conductors per slot, so that for 1 ampere per phase we have the virtual A.T. per pole $=4\times3\times1=12$.

The type of winding is "concentric" on the stator and "barrel" on the rotor, and from Table XVIII. we get $K_L=2{\cdot}45$. Therefore the end leakage per pole is

$$\phi_e=2{\cdot}45\times(31+12{\cdot}5)\times12=1275 \text{ C.G.S. lines.}$$

We will denote by ϕ_e the end leakage per pole when one ampere per phase is passing in the stator winding. Then $I_a\phi_e$ is the end leakage for any current I_a.

As we have seen, the short-circuit current of the motor depends mainly upon the value of the sum of all the leakage fluxes for one ampere passing in the stator.

We will write $\phi_i+\phi_e=\phi_l$, the total leakage per pole for one ampere in the stator.

In the above examples $\phi_i+\phi_e=4135$, and at no load with 90 amperes per phase we have

$$90(\phi_i+\phi_e)=90\phi=3{\cdot}72\times10^5,$$

the total leakage for 90 amperes in the stator.

Then, if ϕ_p is the total flux per pole at normal voltage,

$$\frac{\phi_p}{\phi_l}=I_s, \text{ the short-circuit current,}$$

when normal voltage is applied to the short-circuited motor, assuming that we can neglect the resistance (see page 428).

EXAMPLE 55. In the before-mentioned 1500 H.P. motor with 90 amperes per phase in the stator, the total leakage flux is $3{\cdot}72\times10^5$. The leakage for one ampere is

$$\phi_l=4135.$$

Now the total flux per pole at 3000 volts, $\phi_p=5{\cdot}6\times10^6$. We have then

$$\frac{\phi_p}{\phi_l}=\frac{5{\cdot}6\times10^6}{4135}=1350 \text{ amps.}$$

This is the short-circuit current there would be if there were no resistance. The ratio of the magnetizing current to this I_s is sometimes denoted by τ.

Thus,
$$\tau = \frac{I_m}{I_s},$$

$$\tau = \frac{\text{leakage flux at no load}}{\text{total flux per pole}}.$$

In above example
$$\tau = \frac{3{\cdot}72 \times 10^5}{5{\cdot}6 \times 10^6} = 0{\cdot}067.$$

THE REACTANCE OF THE MOTOR ON SHORT CIRCUIT.

Having calculated the current that would flow if there were no resistance, we can at once get the reactance of the motor regarded as a short-circuited transformer. We can write:

Volts per phase = short-circuit current × reactance per phase.

EXAMPLE 56. In the motor described on page 448, the voltage per phase is 1730, and from the calculation of the leakage flux, the current per phase, if there were no resistance, would be 1350 amperes.

$$1730 = 1350 \times x_a,$$
$$x_a = 1{\cdot}3.$$

THE APPARENT RESISTANCE OF THE MOTOR ON SHORT CIRCUIT.

An induction motor with its rotor locked, that is to say, held so that it cannot turn, and with the rotor circuit closed on itself, behaves like a short-circuited transformer. The apparent resistance observed at the terminals of the primary depends upon the resistances both of the primary and secondary windings. To obtain the effect of the rotor resistance, as observed at the terminals of the stator, it is necessary to multiply the actual resistance of the rotor windings by the square of the ratio of transformation $\frac{S_1}{S_2}$. Let r_2 be the resistance per phase of the rotor winding; then

$$r_{2,1} = \frac{S_1^2}{S_2^2} r_2,$$

as the apparent resistance of the rotor observed at the terminals of the stator.

The total apparent resistance per phase r_A is $r_1 + \frac{S_1^2}{S_2^2} r_2$, where r_1 is the resistance per phase of the stator (see page 456).

THE APPARENT IMPEDANCE OF THE MOTOR ON SHORT CIRCUIT.

Having calculated the apparent reactance of the motor windings per phase (see page 455), we can obtain the apparent impedance by the formula

$$Y = \sqrt{r_A^2 + x_a^2}.$$

EXAMPLE 57. In the 1500 H.P. motor described on page 448, the number of conductors in the stator in series is 864, and the number in the rotor 360. Both are star connected, therefore $\frac{S_1}{S_2} = \frac{864}{360} = 2{\cdot}4$. The resistance of one phase of the stator winding is 0·074 ohm, and the resistance of the phase of the rotor winding is 0·0133. Therefore

$$r_{2,1} = (2{\cdot}4)^2 \times 0{\cdot}0133 = 0{\cdot}0766.$$

Thus $r_A = r_1 + r_{2.1} = 0{\cdot}074 + 0{\cdot}0766 = 0{\cdot}1506$.
And we have found that $x_a \doteq 1{\cdot}3$.
Therefore the apparent impedance

$$Y = \sqrt{0{\cdot}023 + 1{\cdot}69} = 1{\cdot}31$$

The short-circuit current can be obtained by dividing the voltage per phase by the apparent impedance per phase.

EXAMPLE 58. In the 1500 H.P. motor to which the above examples refer, the terminal voltage is 3000, and as the stator is star-connected the voltage per phase is 1730. The short-circuit current $I_{sc} = \frac{E_a}{Y} = \frac{1730}{1{\cdot}31} \doteq 1320$ amperes per phase.

Having calculated the no-load current and the short-circuit current, the Heyland diagram can be constructed as described on page 414, and from it we can obtain the power factor, the slip and the efficiency at various loads—the starting current, the starting torque, the maximum torque and the maximum output.

An example will be found fully worked out in connection with the 1500 H.P. motor described below.

The power factor for various values of τ and various loads. It will be seen that if the ratio between the no-load current I_n and the short-circuit current I_{sc} is fixed, then the power factor for a load forming any specified fraction of the maximum load can be determined from the Heyland diagrams. The only other quantities which would affect the power factor are the resistances of the stator and rotor, and if these are small they affect the result to a very small extent.

In order to be able to state what the power factor of any motor will be at any particular load, without going through the calculation, it is convenient to have curves such as those given in Fig. 405. Each curve is drawn for a different value of τ, where

$$\tau = \frac{\text{leakage flux at no load}}{\text{total flux per pole.}}$$

or

$$\tau = \frac{\text{magnetizing current}}{\text{wattless component of short-circuit current}}.$$

As the resistance of the stator and rotor windings does have some effect upon the power factor, the curves have been drawn on the assumption that the power factor on short circuit is 0·25; that is to say,

$$r_A = 0{\cdot}25 x_a.$$

This is not so far from the truth in many commercial motors as to call for any correction of the curves where the power factor is less than 0·25 on short circuit. Where, however, the power factor on short circuit is as great as 0·5, we should use the curves as if the values of τ attached to each curve were increased 10 %. Thus, power factor $= 0{\cdot}5$ on short circuit, and the top curve should be used for $\tau = 0{\cdot}022$.

The crawling of induction motors. When a squirrel-cage motor is being started up it sometimes attains about one-seventh of full speed and refuses to go any

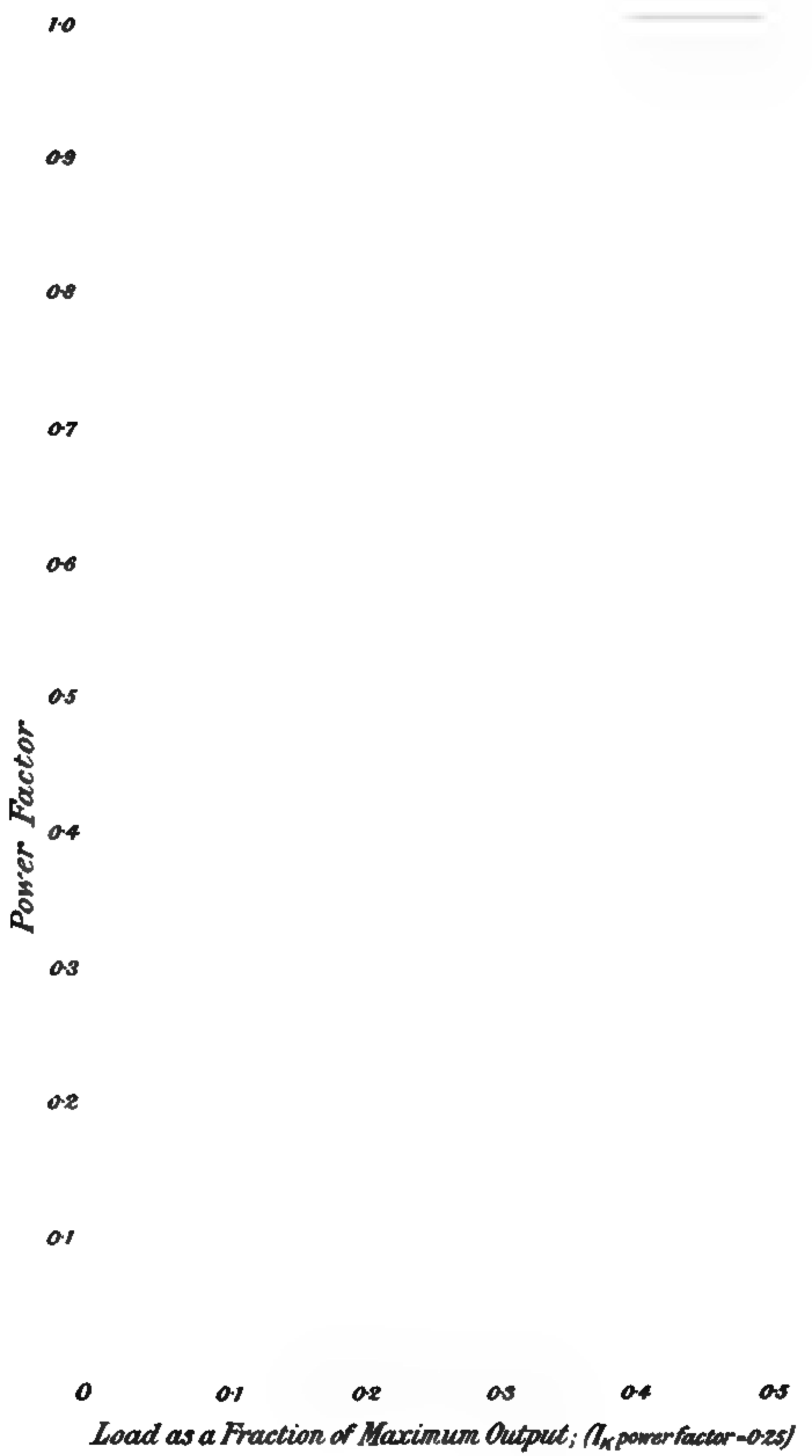
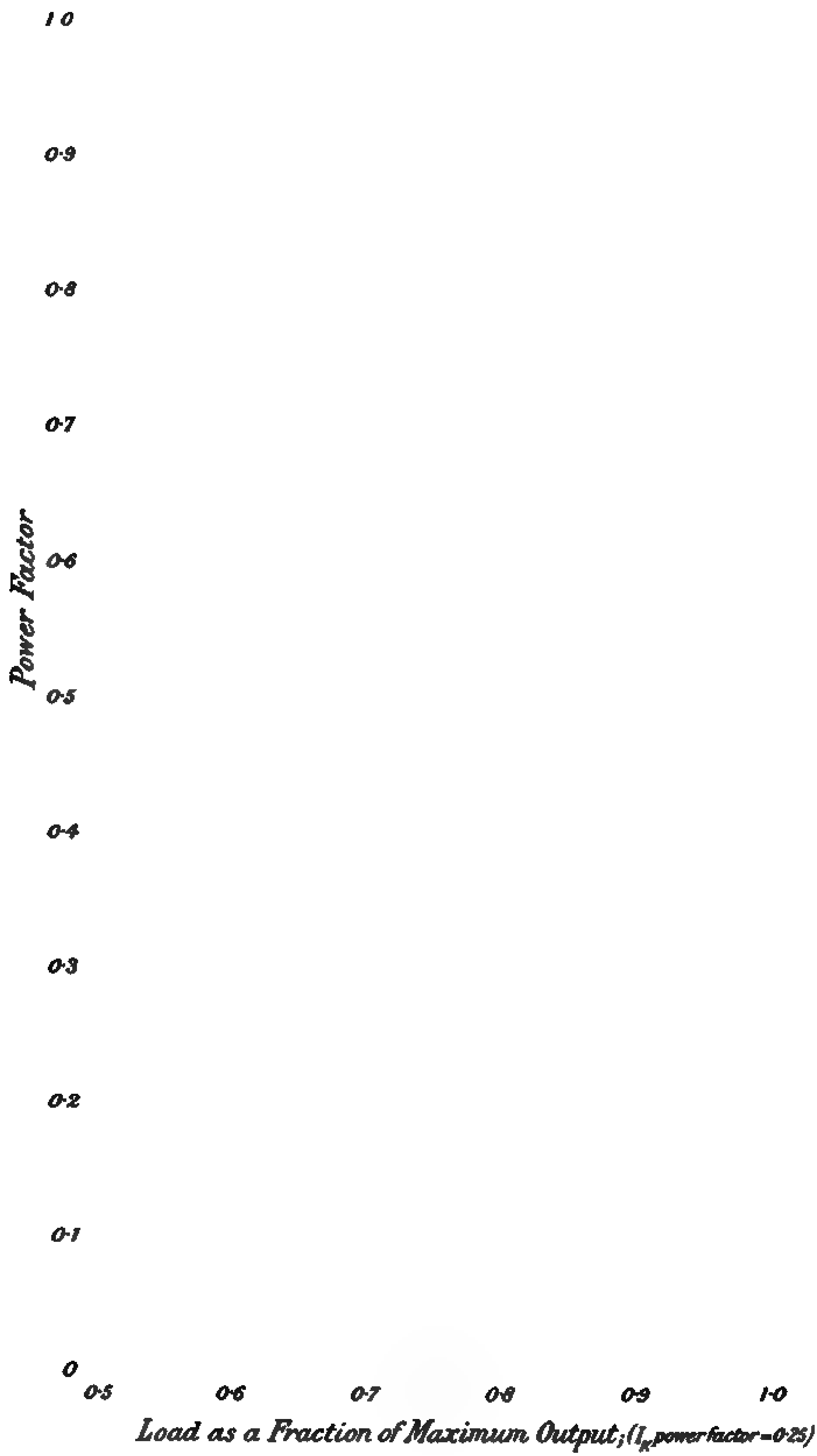


FIG. 405.—Curves giving the power factor of an induction motor



when loaded with any given fraction of its maximum load.

faster, until by some means the speed is carried over the dead point, when the torque of the motor increases and it runs up to full speed. This is due to the presence of a pronounced seventh harmonic in the field-form (see page 22). The harmonic has the effect of superimposing on the main field, and the field having seven poles within the span of one main pole pitch. As these poles alternate with the frequency of the supply (say 50 cycles), they produce a torque on the rotor having the same characteristics as the torque produced by a motor having seven times as many poles, and having a synchronous speed one-seventh of the normal speed. The whole torque on the rotor is thus made up of the torque, due to the main revolving field plus the torque due to the seventh harmonic.

Fig. 406 shows the speed-torque curve of such a motor. The torque is greatest when running slightly under synchronous speed. If we drive the motor above

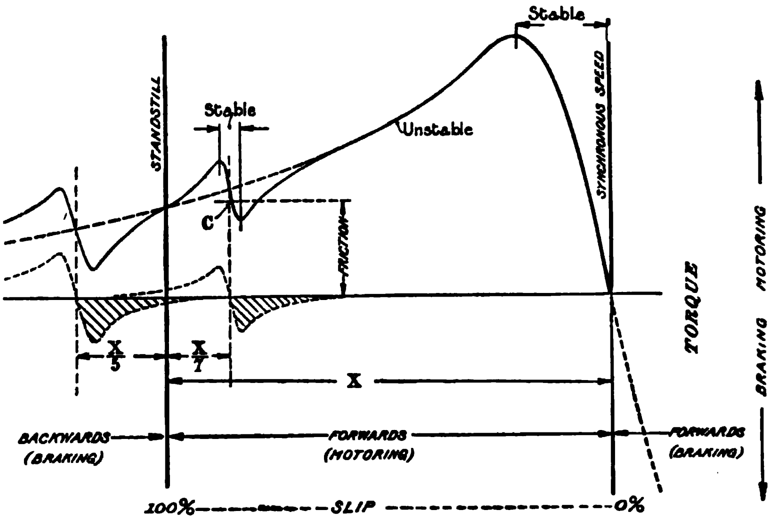


FIG. 406.—Showing how the torque-speed characteristic of a squirrel-cage motor is affected by harmonics in the field-form.

synchronous speed, the torque becomes negative. Now the torque due to the seventh harmonic is similar in shape, as is shown by the dotted line which crosses the zero line at one-seventh of full speed. If this dotted curve be superimposed upon the main characteristic, we get the curve shown by the full line. We see, therefore, that as the motor starts from rest the torque increases up to the little peak on the curve, and then rapidly diminishes as we approach what is the synchronous speed for the seventh harmonic. Above that speed the torque still diminishes, because, so far as the harmonic is concerned, we are getting a braking action. It is not until we have increased the speed by an amount which takes it well past the seventh speed that the torque again begins to increase. If, now, the friction of the motor should be such that it requires a torque greater than that supplied by the motor in the region of the seventh speed, the motor will crawl round and be perfectly stable in maintaining its speed between certain limits. If we have a fifth harmonic

on the field-form, it tends to produce rotation in the opposite direction, and gives to the motor the characteristic shown in Fig. 406. These matters are very lucidly discussed by Mr. Catterson Smith in a paper* from which Fig. 406 is taken.

In order to avoid this crawling of squirrel-cage motors it is necessary to make the resistance of the end rings of the cage great enough to give to the motor at seventh speed a torque well above the friction torque.

Slip of an induction motor. Although the slip is given by the ratio of $XY : PY$ in Fig. 400, it is not convenient to calculate the slip by scaling off these vectors, because XY is too small to be accurately measured. A much more accurate way is to calculate the ohmic losses in the rotor from the known resistance and the known current. For this purpose we may either take the current by scaling off PN (in which case the ohmic loss will be $3PN^2\, r_{2,1}$), or we may multiply the current PN by the ratio of transformation S_1/S_2, and obtain the true rotor current I_2 (in which case the ohmic loss will be $3I_2^2 r_2$). In this we have taken r_2 as the resistance of one leg of the star-connected rotor winding. Having obtained the ohmic loss in the rotor $3I_2^2 r_2$, we obtain the slip from the formula:

$$\text{Slip} = \frac{3I_2^2 r_2}{\text{Output in watts} + 3I_2^2 r_2}.$$

Examples are given on pages 459 and 467.

* Catterson Smith, *Journal Inst. Electrical Engineers*, vol. 49, p. 635, 1912.

CHAPTER XVII.

THE SPECIFICATION OF INDUCTION MOTORS.

THE uses to which induction motors are put are so very various, and the performance required in the various cases so very different, that it is difficult to find a wholly satisfactory classification when we come to treat of the subject in a systematic way. Broadly, there are two classes: (1) motors that are started and stopped only once or twice a day, and run at an almost constant speed; and (2) motors that are started and stopped or reversed frequently, and may be required to run at various speeds. The first class we find driving machines that are running all the time on steady or varying loads, such as motor generators, pumps, the counter-shafts in small factories, the main shafts in large mills that have been converted to electrical driving, cotton-spinning machinery and flour mills. The second class we need for machine tools that are often started and stopped, cranes, lifts and winding engines. An intermediate class, so far as service is concerned, is formed by the motors which, while running all day in one direction, have their speed changed over a wide range, as, for instance, non-reversing rolling-mill motors, which must permit a flywheel to give up a large part of its energy.

Small motors for the first class of service are often made of the squirrel-cage type with low-resistance rotors and high efficiency. They are usually started up without much load, either by means of an auto-starter or by connecting the windings of the stator first in star and afterwards in mesh. Such motors start up on about full-load current when provided with an auto-starter, and on less than three times full-load current with the star-mesh system. Where a small motor has to be started up on load the squirrel cage can still be used, if the amount of current drawn from the line does not matter, or if the efficiency does not matter. In the latter case the squirrel cage is made of fairly high resistance, so as to give a good starting torque. Large motors for constant speed, if they can be started light, are sometimes made of the squirrel-cage type. But the advantage of this type in the case of very large motors is doubtful. The mechanical construction of the squirrel cage to deal with very large currents, and provide properly for expansion and contraction, is hardly any easier or cheaper than a barrel winding, and the starting with a wound rotor is so much more satisfactory that most large motors are now made of that type. Even when the motor is started up independently (as in a motor-generator started on the continuous-current side) and switched on at full

speed, we do not get rid of shocks at the instant of switching unless we employ a charging coil or water resistance.

For crane work and for motors for hard service in dirty situations, where the efficiency is not of first importance, squirrel-cage motors with high-resistance rotors are sometimes employed, but the methods now used of enclosing motors and slip-rings are so efficient, and the construction of wound rotors so hardy, that there is now a great deal to say in favour of external resistances instead of the high-resistance squirrel cages.

For the second class of service, rotors with a wire or bar winding, and provided with slip rings, will be used to enable the resistance of the rotor to be increased at starting and when running much below synchronous speed. Motors of this type can be started up on full-load torque with not much more than full-load current, and at nearly full-load power factor. Where desired, the starting torque can be made three or four times the full-load torque, with a corresponding increase in the starting current. To call for more than three times full-load torque usually involves paying the price of a motor built on a larger frame, and there will be a consequent loss in efficiency.

General. The specification, besides stating the voltage and frequency of supply, should give such particulars of the source of power and the other apparatus in circuit as may be necessary to enable a manufacturer to judge whether his motors are suitable for the circuit in question. If the motor is to be run from town mains, there will generally be some restriction as to the current that may be drawn and its power factor. If there is a long feeder in circuit likely to cause a serious drop in the voltage on load, the fact should be stated. Particulars should be given of the general character of the load and the probable accuracy with which the horse-power and maximum torque have been estimated.

Starting. The method proposed for starting the motor should be stated, together with the starting torque required and the amount of current that may be drawn from the line. The power factor of the motor at starting is a very important characteristic affecting the cost of the installation. If the motors are small, as compared with the whole power of the circuit to which they are connected, and there are no special circumstances which call for a good power factor on starting, it is not well to insist on too stringent conditions, or the complication and cost of the plant may be unduly increased. It is, for instance, quite a common practice to start up 30 and 40 H.P. squirrel cage motors in a large factory on simple auto-starters or on the star-mesh method, and though the rush of current at low power factor is considerable, it does not affect the good working of the whole factory, and the plant is simpler than if slip-ring motors had been employed. The maximum torque required should be stated with as great accuracy as possible. It must on no account be understated, because the maximum torque which an induction motor can give is a fairly definite quantity, and cannot be increased by merely over-loading the motor as with a direct-current motor. On the other hand, it should not be overstated, or the manufacturer will supply a motor which is too large for the work, so that the efficiency and power factor will not be as good as they might be.

Speed. If there are any reasons why the speed must be kept exceedingly constant, the fact should be stated. Usually a statement of the purpose for which the motor

is required is sufficient information for the manufacturer to go upon in adjusting the slip of the motor, and it is only in special cases that it is necessary to specify the slip exactly.

Where wide variations in speed are required, the range of speed should be given as accurately as possible, and a statement should be made whether the change of speed must be continuous over the whole range, or whether it is permissible to introduce pole-changing, gear-changing, or any devices which will increase the efficiency at certain speeds, or in any way reduce the difficulty of obtaining the wide range of speed.

Power factor. It is usual to permit the manufacturer to specify the power factor of his motor. It is sufficient for the purchaser to indicate the relative importance of a good power factor in his particular case. The manufacturer can then choose a standard motor to meet the case, without being compelled to alter the windings to aim at some particular figure. It should not be forgotten that where a good power factor is of the greatest importance, devices can be added to the motor which will make the power factor unity or even leading (see page 605).

Maximum torque. The maximum running torque which a motor is to yield is a very important consideration in determining the size of the frame upon which it must be built, and therefore the price of the motor will largely depend upon the maximum torque required. For general work a torque of $2\frac{1}{2}$ times the running torque is considered sufficient to prevent accidental pull-outs, but the circumstances of each case should be considered, and where severe over-loads are likely to come on, the motor must be designed to meet them. In some cases the load may be so steady that a much smaller maximum torque may be sufficient, and a saving can be made both in the cost of the motor and in the power taken to drive it.

Temperature rise. What was said on page 256 about temperature rise is also applicable to induction motors. With crane motors of the squirrel-cage type, provided with high-resistance end rings, it is usual to allow the temperature to rise to 150° to 200° C., the construction being specially designed to withstand these temperatures without injury.

Puncture test. The rules with regard to puncture test are in general the same for the stator of an induction motor as for the armature of a generator. In cases where it is intended to switch an idle motor directly on to a high-voltage line, special care is necessary in the insulation between turns of the coils of the stator nearest the terminals. In such cases it may be necessary to specify a certain test between turns on these coils, the test to be made during the course of construction. If the insulation is designed to resist an instantaneous puncture test between turns of one-half the voltage of the motor, it will in general be sufficient to withstand being switched suddenly on to the line.

Arrangement of frame and shaft. The specification should state any matters relating to the arrangement of the frame which are important for the installing of the motor. It should state, for instance, whether the motor must go on its own bedplate, or whether the frame must be designed to fit some special support.

The bringing out of the terminals is a matter which in some cases requires special consideration, particularly with motors that are to be put in rather inaccessible places. The specification should draw attention to any points of this kind.

Then, again, it sometimes happens that a motor requires special protection from dirt or dripping water. Protection on one side may be sufficient, or a total enclosed motor may be required.

The specification should also give particulars as to how the motor is to be connected to the load, whether by pulley and belt or spur gear, and of the sizes of these. If the motor is to be direct coupled, particulars should be given of the kind of coupling and its size, and a statement made as to how much of the coupling is to be supplied by the manufacturer of the motor. Sometimes it is necessary to have the shaft of extra length, or turned to a special size or shape. Matters of this kind should always appear on the specification, as they have a very considerable effect upon the cost of manufacture.

SPECIFICATION No. 7.

1500 H.P. INDUCTION MOTOR.

Extent of Work.

85. This specification covers the manufacture, supply, delivery, erection, testing, and setting to work of a three-phase induction motor, direct connected to a 1000 K.W. continuous-current generator in the Sub-station of the Corporation in Street,

Function of Motor.

86. The motor is intended to drive an existing continuous-current generator of 1000 K.W. capacity at a speed of 246 revolutions per minute. The said generator feeds the mains of the power and lighting supply of the town of with continuous current at a pressure of 500 volts.

Type of Rotor.

87. The rotor shall be of the wound type provided with slip-rings.

Characteristics of Motor.

88. The motor is to have the following characteristics:

Normal output	1500 H.P.
Normal voltage at terminals	3000 volts.
Frequency	50 cycles.
Number of phases	3.
Speed	246 revs. per minute.
Power factor not less than	*
How connected to load	Direct connected through flange coupling.
Temperature rise after 6 hours full load run	40° C. by thermometer.
Over load	25 per cent. for 3 hours.
Temperature rise after 3 hours 25 per cent. over load	55° C. by thermometer.
Maximum torque	$2\frac{1}{2}$ times full-load torque.
Starting torque	Sufficient to start the 1000 K.W. generator unloaded.

* The Contractor is to state the power factor at full load of the motor he proposes to supply.

Puncture test	6600 volts alternating at 50 cycles applied for 1 minute between the stator windings and frame.
	3000 volts alternating at 50 cycles for 1 minute between rotor windings and frame.

89. The contract includes the delivery of the motor at the Sub-station of the Corporation, together with bedplate, bearings and pedestals, and the erection, aligning and coupling of the same to the 1000 K.W. generator. The switch gear and starting gear are provided for under another specification. **Extent of Work.**

90. The stator frame shall be of the best cast-iron, of deep section and great stiffness, so as to prevent any appreciable distortion due to magnetic pull. The finger-plates supporting the ends of the stator and rotor teeth shall be of very rigid construction, and shall be approved by the Purchaser. **Stator Frame.**

91. The parts of the stator coils projecting from the slots shall be so rigid that no appreciable movement of them occurs under the most severe conditions of service. **Stator Coils.**

91*a*. Each coil shall be wound so that those conductors between which the highest potential occurs are furthest separated from one another. **Arrangement of Conductors.**

92. The stator shall be fitted with strong guards or fenders to prevent the H.T. windings being accidentally touched by hand. These fenders shall be designed so that they do not interfere with the ventilation. **Fenders.**

93. The individual conductors forming each stator coil shall be insulated with mica bound in position in an approved way. The coils shall be dried and impregnated under vacuum with insulating compound; they shall then be wrapped on the straight portions with mica mounted on cloth or paper, the whole moulded under pressure so as to exclude air-spaces. The ends of the coils shall be insulated with tape treated with a suitable insulating varnish of the highest quality. The stator slots shall be lined with paraffined fullerboard to prevent abrasion of the tape when the coils are inserted. The whole of the insulation shall be carried out so as to be **Insulation of Stator Coils.**

permanent and reliable, and so as to withstand well the heating and vibration to which the coils may be subjected. After being placed in the slots, the coils shall be completely covered with a non-hygroscopic waterproof varnish capable of withstanding the action of hot oil, and having a smooth surface which will neither soften nor crack under working conditions.

Pressure Tests. 94. All pressure tests hereinafter specified shall be carried out with an alternating voltage at a frequency of 50 cycles per second.

Tests on Stator Winding. 95. Before being placed in the slots, each stator coil shall be tested at 2000 volts between each pair of adjacent turns. After the coils have been placed in the slots and before they are connected up, the whole of the coils shall be subjected for 1 minute to 8000 volts between copper and iron. After the coils have been connected up, but before the phases are interconnected, they shall be tested between phases A and B, B and C, and C and A at a pressure of 8000 volts. After the motor has been delivered and run on full load for 6 hours, it shall, while still warm, be subjected to a pressure of 6600 volts, for 1 minute, between stator copper and iron.

Rotor. 96. The rotor shall be built upon a cast-iron spider with free arms, designed to avoid excessive stresses arising through the cooling of the casting. The rotor winding shall consist of copper bars which are insulated before being placed in the slots; each bar shall be insulated on the straight portion by mica mounted on paper or cloth, and held in position by tape. The end portions shall be insulated with Empire cloth and treated cotton tape. The insulation of the bars shall be completely impregnated with varnish and well dried out. The slots shall be lined with paraffined fullerboard to prevent abrasion of the tape when the coils are inserted.

Tests on Rotor Coils. 97. After the rotor bars have been connected together, but before they are connected in star or in mesh, a pressure of 4000 volts shall be applied for 1 minute between phases A and B, B and C, and C and A. The phases shall then be connected in star or in mesh, and a pressure of 3000 volts shall be applied between copper and iron. This test shall be repeated after the motor has been run at full load for 6 hours and while it is still hot.

98. The tender shall state the radial clearance between the stator and rotor iron. **Air-gap.**

99. The efficiency of the motor shall be calculated from the separate losses, which shall be measured in the following way: **Efficiency.**

1. *Iron loss, friction and windage.* The machine shall be run unloaded at 3000 volts between terminals, and the power taken to drive it measured by the two-wattmeter method. The sum of the readings shall be taken to be the iron loss, friction and windage.

2. *Copper losses.* The rotor shall be locked and the rotor windings short-circuited through suitable ampere-meters. A voltage shall then be applied to the stator at 25 cycles * per second and gradually brought up until the rotor winding yields full-load current on short circuit. Measurement shall be made of the power supplied to the stator under these conditions by means of two-wattmeter readings; the sum of these readings shall be taken to represent the copper losses at full load.

100. The Contractor shall guarantee the efficiency calculated from these separate losses, and he shall further guarantee that when the motor is in operation the over-all efficiency actually obtained shall not be more than 1 per cent. lower than the efficiency so calculated. The figures for the calculated efficiency shall be given at full load, three-quarter load and half load. **Guarantee of Efficiency.**

101. In addition to the puncture tests specified in Clauses 95 and 97, the following tests shall be made at the maker's works: **Tests at Maker's Works.**

1. Iron loss, friction and windage test as specified in Clause 99.

2. Copper loss test as specified in Clause 99.

3. Resistance test. The resistances of the rotor and stator windings shall be measured.

102. The following tests shall be carried out after the motor is erected in the Sub-station of the Corporation: **Tests on Site.**

1. *Temperature test.* The motor shall be run for 6 hours at full load, which for this purpose shall be taken to mean

* The reason for specifying that this test shall be carried out at one-half the rated frequency is that the losses on the rotor at a high frequency would be unduly increased.

an input of 1200 K.W. at 3000 volts. At the end of this run the motor shall be stopped and temperatures taken with all possible speed. The temperature of any part of the motor, as measured by a thermometer, shall not rise more than 40° C. above that of the surrounding air. For this purpose the temperature of the air shall be taken to be the temperature measured in line with the shaft of the motor at a distance of 3 feet from the end of the shaft.

2. *Over-load test.* Immediately after taking the temperatures, the motor shall be put for 3 hours on over load, which for this purpose shall be taken to be an input of 1500 K.W. at 3000 volts; after which run the temperatures shall be taken again. The highest temperature rise shall not exceed 55° C.

3. *Power-factor test.* During the temperature run the power factor of the motor shall be measured by means of a power-factor meter, and also by the two-wattmeter method.

Starting. 103. The motor will be started with a starting resistance described in another specification, connected in series with the rotor.

Power Factor at Starting. 104. The current drawn from the line shall not at any time during the starting exceed full-load current, and the wattless current shall not exceed half full-load current.

Slip Rings. 105. The slip rings on the rotor shall be of substantial design, and shall have a wearing depth of not less than $1\frac{1}{2}$ in.

Brush Gear. 106. The brackets supporting the brush gear shall be of very rigid construction. The brush holders shall constrain the brushes so that they always slide in a direction parallel to the same line. The brushes shall be of the metal-carbon type by a good maker, and they shall be fitted with a flexible connection capable of carrying the current on 25 per cent. overload without undue heating. There shall be absolutely no sparking on the slip rings when the machine is running on 25 per cent. overload.

Short-circuiting Device. 107. The brush gear and slip rings shall be provided with a device whereby the slip ring may be short circuited when the motor is up to speed, without the current passing through the brushes.

108. The design of the fenders around the stator winding and the shaping of other parts of the motor shall be such that the air thrown out by the rotor shall be thrown out well to the surrounding atmosphere, and shall not to any appreciable extent be thrown toward the continuous-current motor, or be caused to circulate in an eddy so as to return on the motor itself. While ample ventilating ducts must be provided in stator and rotor, these must be designed so as not to create excessive noise. Ventilation.

(See Clauses 6, p. 271; 36, p. 360; 74, p. 382; 272, p. 591.) Foundations.

(See Clauses 55 to 59, page 379.) Accessibility of Site.

(See Clauses 8, p. 271; 60, p. 379; 273, p. 591.) Use of Crane.

109. The rotor shall be well balanced, and when at full speed shall not communicate to the bearing pedestals any appreciable vibration. Balance.

110. The shaft of the rotor shall be fitted with a half coupling (forged with it) for coupling to the shaft of the continuous-current generator. The coupling bolts and nuts shall be completely covered by a steel shrouding. All coupling holes shall be reamered out in position, and well-fitting finished bolts and nuts shall be supplied. After fitting, each coupling shall be clearly marked for correct matching when re-erecting. Coupling.

(See Clauses 67, p. 380; 268, p. 590.) Bearings.

111. The motor shall be mounted on a bedplate and two pedestal bearings. The bedplate shall be arranged to be bolted to the existing bedplate of the continuous-current generator, particulars of which are given in drawing No. . The height of the pedestal shall be arranged so as to bring the rotor shaft in line with the existing shaft of the continuous-current generator. Bedplate.

(See Clause 186, page 523.) Holding-down Bolts.

112. The position of the terminals shall be shown on the tender drawings. The connections to the high-tension cables shall be made by means of sweated thimbles, which shall be completely insulated by means of substantial insulating sleeves. The cable which makes connection between the Terminals.

stator winding and the terminals shall be insulated with Empire cloth or other insulation which does not soften with heat.

Or

113. The terminals of the stator winding shall be brought by means of cables insulated by Empire cloth, or other insulation which does not soften with heat, to a cast-iron terminal box, in which the terminals shall be mounted on independent porcelain insulators. Wide and efficient insulating screens shall be placed between the terminals of the three phases, so that arcing between phases is impossible. The connections from the high-tension switchboard will be made by means of a three-core paper-insulated cable, which will be brought to a trifurcating box designed to fit on to the terminal box aforesaid, for convenient connection between the high-tension cable and the terminals of the stator.

Spares. 114. The Contractor shall supply the spare parts set out in Schedule I.

Tools. 115. The Contractor is to provide a full outfit of the spanners and special tools necessary for disassembling and assembling the motor, together with a rack for holding them.

Maintenance Period. (See Clauses 138*b*, p. 469; 206, p. 528.)

Cleaning and Painting (See Clause 209, page 528.)

Drawings supplied with Specification. 116. Drawing No. supplied with this specification shows the existing lay-out in the Sub-station of the Corporation and the proposed site for the induction motor.

117. Drawing No. gives particulars of the existing continuous-current machine, with bedplate and outboard bearing, to which it is proposed to connect the induction motor.

Contractor to make Measurements. 118. The Contractor is advised to inspect the site and make all necessary measurements. The Contractor is to be responsible for obtaining any information which shall be necessary for him in deciding as to the suitability of the site for his plant, and also for the exact dimensions of all bedplates, bearings, heights, clearances and foundations, and other matters with which he may be concerned.

119. Schedule No. II. gives a list of the drawings and samples which are to be submitted with the tender. **Drawings to be supplied with Tender.**

(See Clause 212, page 529.) **Provisional Sum.**

SCHEDULE No. I.

LIST OF SPARES.

SCHEDULE No. II.

LIST OF DRAWINGS REQUIRED WITH TENDER.

1. Outline drawings showing in plan and elevation the dimensions of the proposed motor coupled to the existing C.C. generator.

LIST OF SAMPLES REQUIRED WITH TENDER.

1. Sample of stator coil, showing method of insulating between copper and iron and the method of insulating the bent part of the coil. The coil shall also show the method of making joints in the conductors.
2. Sample of rotor bar with its insulation.
3. Sample of brush holder and brush for slip rings.

DESIGN OF 1500 H.P. INDUCTION MOTOR.

3000 volts, 50 cycles, 246 R.P.M., power factor at full load 0·88. At an efficiency of 95 % this gives 1350 K.V.A.

We will suppose that it is required to design a motor to comply with the particulars given in the specification No. 7 (page 438). The first step is to fix upon a suitable size of frame. In practice, a manufacturer would probably use a frame for which drawings were already in existence, and upon which he had built similar motors before; but if he had no such frame he would take as a guide a suitable output coefficient upon some such considerations as the following.

The **output coefficient** of induction motors depends upon a variety of considerations, some of which are the following: the rated output,—the ratio of the maximum output to the rated output,—the number of poles,—the temperature rise,—the facilities for ventilation,—the cost,—the efficiency,—and the power factor at full load. As the interaction of these factors is extremely complicated, it is impossible

to give any concise rules for arriving at the output coefficient in any particular case. If we took a frame of certain size and wished to get the **maximum pull-out torque** without regard to anything else, we would build an " iron " machine; that is to say, we would make the slots very small and shallow to leave room for the working flux, which would be made as great as possible. The leakage flux being very small, the diameter of the semi-circle (Fig. 410) and the maximum output would be very great. As, however, the room for copper would be very small, the output at which the motor would work in continuous service would be small. To get a **greater continuous output** we would increase the size of the slots. This would restrict the amount of the working flux and increase the leakage, so that the maximum output would be reduced, while the rated output would be increased. This would go on until we arrived at a size of slot such as is commonly found in practice. If the size of slot were still increased, we would have to cut down the working flux by a percentage higher than the percentage increase in the current loading, so that the normal output of the motor would be decreased. With the ratio of current loading to magnetic loading commonly found in 50-cycle motors of normal speed, the ratio of the maximum output to rated output is about 2 to 2·5. This ratio is found to be quite satisfactory for the ordinary purposes for which motors are employed, and it gives a fairly economical arrangement of copper and iron. For very small motors (from $\frac{1}{2}$ to $1\frac{1}{2}$ H.P.) the economical ratio is still smaller.

For a given frame and given frequency an increase in the **number of poles** affects the output coefficient in two ways. The speed being lower, the ventilation is not so good, and this tends to reduce the output; but, on the other hand, it is found that, the pitch of the poles being shorter, the number of coils huddled together is fewer, and this more than compensates for the slower speed, particularly where fans are added. A comparison of the output coefficients of modern well-ventilated motors will show that upon the whole an increase in the number of poles increases the ratio, $\frac{\text{output}}{\text{speed}}$, instead of decreasing it, as might at first be supposed from the poorer draught of air. This will be seen from Table XIX.

The **temperature rise** guaranteed, of course, affects the size of frame that must be employed. In what follows we will assume that the guaranteed rise is 40° C. by thermometer. The **facilities for ventilation** differ very widely with the purposes for which the motor is intended. Pipe-ventilated motors, for instance, are greatly dependent on the size of the pipe carrying the air and the pressure used to drive it. In what follows we will assume that the motor is ventilated with its own fan, and that there is a plentiful supply of cool air.

Considerations of **cost** affect the output coefficient, because it does not by any means pay to make the *smallest* possible motor to meet the guarantees. Iron is cheaper than copper, and it will pay better to make a rather bigger " iron " motor than the smallest possible " copper " motor.

Similarly, the **efficiency** and **power factor** required will often compel us to use a frame of larger size than might otherwise be necessary.

If we take modern, well-ventilated, standard, 50-cycle motors, running at speeds that are ordinary for the output, we will find that the ratings of the frames

are such as to lead to the coefficients K_0 given in Table XIX. The increase in the number of poles on these standard machines will have the effect of reducing the maximum torque, so that while we find that the coefficient K_0 is increased slightly as the number of poles is increased, the maximum load of the frame is at the same time decreased. If it should be necessary to build a slow-speed motor and still preserve the large pull-out torque, it will be necessary to take a frame of larger diameter, so as to get more room for increasing the number of slots and the working of flux per pole. This will have the effect of reducing K_0. In Table XIX. the coefficient is based on heating considerations as found on standard motors.

TABLE XIX. RATINGS OF FRAMES OF 50-CYCLE, 3-PHASE INDUCTION MOTORS.

$$K_0 \times D_m^2 \times l_m \times \text{R.P.M.} = \text{K.V.A. input.}$$

D_m = diameter of rotor in metres; l = axial length of iron in metres; K_0 is a coefficient * applicable under the circumstances set out above.

	4 Poles.	6 Poles.	8 Poles.		12 Poles.		16 Poles.	24 Poles.
K.V.A.	K_0	K_0	K_0	K.V.A.	K_0	K.V.A.	K_0	K_0
1	0·4	0·4	0·4	50	1·6	100	1·8	1·85
2	0·55	0·57	0·6	100	1·7	200	1·95	2·0
5	0·75	0·85	0·9	200	1·8	500	2·05	2·1
10	1	1·1	1·15	500	1·9	1000	2·15	2·2
20	1·15	1·25	1·35	1000	1·95	1500	2·15	2·2
50	1·3	1·45	1·55	1500	2·0	2000	2·15	2·2

The continuous output of a frame can be increased beyond ratings arrived at by the coefficient given, by departing from the supposed conditions. For instance, if we have a motor with few poles which, with ordinary design, gives us a much greater maximum load than is required for the purpose in hand, it may be possible on that motor to deepen and widen the slots, and to increase the continuous output at the cost of the maximum output. An example of this will be found in the 350 H.P. motor, particulars of which are given on page 463. As this motor is not required to give more than 1·5 times full load, sufficient copper is put into the stator and rotor to enable it to run continuously on full load at a rating considerably higher than would be chosen ordinarily for a frame of this size.

Table XIX. is given for 50-cycle motors. For 25 cycles a motor for the same speed will have half the number of poles, and the reduction in the number of poles reduces K_0, if the sizes of the slots remain as before. On the 25-cycle motor the pole pitch will be twice as great as for a 50-cycle machine; and it will generally be good practice to make the slots considerably deeper than we should on a 50-cycle motor, because we can do so without making the leakage excessive. It is thus possible to get a larger number of ampere-wires per cm. of periphery on a 25-cycle motor of the same output, speed and power factor, than on a 50-cycle motor.

* Note that D_m and l_m are in metres, while D and l given on the calculation sheets are in centimetres. To arrive at the D^2l constant, as given on the calculation sheets, we must multiply the reciprocal of K_0 by 10^6. Thus, for $K_0=2$, we have

$$\frac{D^2l \times \text{R.P.M.}}{\text{K.V.A.}} = \frac{10^6}{K_0} = 500{,}000.$$

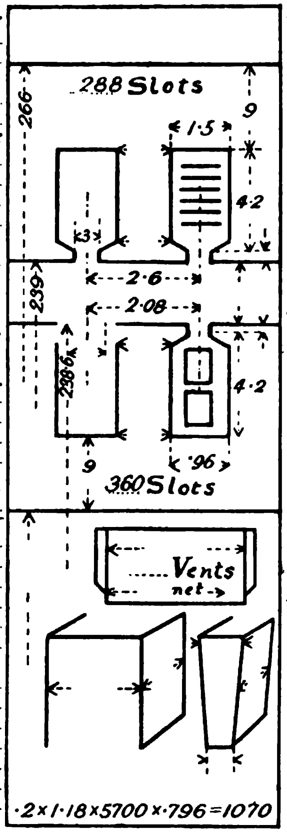
Date 6 May 1913 Type ~~GEN.~~ ASYN MOTOR ~~ROTARY~~ 24 Poles ... Elec. Spec. 7
K.V.A. 1350 ; P.F. .88 ; Phase 3 ; Volts 3000 ; Amps per ter 262 ; Cycles 50 ; R.P.M. 246 ; Rotor Amps 520 ;
H.P. 1500 Amps p. cond. 131 ... Amps p. br. arm ... Temp. rise 40° C ... Regulation ... Overload 25% 3 hours

Customer ... ; Order No. ... ; Quot. No. ... ; Perf. Spec. ... ; Fly-wheel effect ...

Frame 266/47 Circum. 750 ; Gap Area 35200 ; poss. $A_g B$ ... ; poss. $I_a Z_a$ ... ; $I_a Z_a$ ... ; $\frac{D^2 L \times RPM}{K.V.A.}$ 4·9 × 10⁵ ;
Air ... ; $A_g B$ 2·04 × 10⁸ ; $I_a Z_a$ 2·24 × 10⁵ ; Circum. 300 ;

K_a ·41 ; 3000 Volts = ·41 × 4·16 × 864 × 2·04 ; Arm. A.T. p. pole ... Max. Fld. A.T. ...

Armature. ~~Rev.~~ Stat.			
Core.	Dia. Outs.	266	
	Dia. Ins.	239	
	Gross Length	47	
	Air Vents 7 : 0·8	5·6	
	Opening Min. ... Mean		
	~~Air~~ Velocity	31 m per sec	
	Net Length 41·4 × ·89	37	
	Depth b. Slots	9	
	Section 332 Vol.	266,000	
	Flux Density	8450	
	Loss ·044 p. cu. cm. Total	9000	
	Buried Cu. 7500 Total	16,800	24,300
	Gap Area 35,200 ; Wts	12,000	
	Vent Area 48,000 ; Wts	4700	
	Outs. Area 60,000 ; Wts	9000	25,700
Teeth.	No of Segs 12 Mn. Circ.	760	
	No of Slots 288 × 1·5 =	432	
	K_s	328	
	Section Teeth	12,100	
	Volume Teeth	52,000	
	Flux Density	16,900	
	Loss ·15 p. cu cm Total	7800	
	Weight of Iron	2100 kgs.	
Conductors.	Star ~~or Mesh~~ Throw	1-16, 2-15, 3-14	
	Cond. p. Slot	6	
	Total Conds	864	
	Size of Cond. ·38 × 1	·37 sq. cm.	
	Amp. p. sq.	354	
	Length in Slots 47		
	Length outside 65 Sum	112	
	Total Length	1930 m	
	Wt. of 1,000 330 Total	640 Kilog.	
	Res. p. 1,000 ·46 Total	·89 ÷ 4 ÷ 3 = ·074	
	Watts p. metre	58	
	Surface p. metre	1040 sq cm.	
	Watts p. Sq. cm.	·054	
	·054 × ·26 = ·0012	11·7° C.	



~~Field Stat or~~ Rotor.		
Dia. ~~Bore~~	238·6	
½ Total Air Gap	0·2	
Gap Co-eff. K_c	1·18	
Pole Pitch 31·2 Pole Arc		
K_f	·66	
Flux per Pole	5·6 × 10⁶	
Leakage n.L. ... f.L.		
Area ... Flux density		
Unbalanced Pull	0	
No. of Seg. 12 Mn. Circ.	735	
No. of Slots 360 × ·96 =	345	
Vents 7 ·8 cms.	390	
K_s ... Section	14400	
Weight of Iron	1850 kgs	

	Shunt.	Series.	Comm.
A.T. p Pole n. Load			
A.T. p. Pole f. Load			
Surface			
Surface p. Watt			
I². R.			
I. R.			
Amps.	520		
No. of Turns			
Mean l. ~~Turn~~ cond.	95 cms		
Total Length	685		
Resistance	·16 ÷ 4 ÷ 3 = ·0133		
Res. per 1,000	·234		
Size of Cond.	·5 × 1·5	·73 sq. cm.	
Conds. per Slot	2		
Total	720		
Length			
Wt. per 1,000	630		
Total Wt.	430 kgs		
Watts per Sq.	·066		
Star or Mesh			
Paths in parallel	2		

Magnetization Curve.	Section.	Length	... Volts. B.	A.T.p	A.T.	2950 Volts. B.	A.T.p.cm	A.T.	... Volts. B.	A.T.p.	A.T.
Core		10					3	30			
Stator Teeth	12,100	4·2				16,500	52	220			
Rotor Teeth	14,400	4·2				14,000	14·5	60			
Gap	35,300					5700		1070			
Pole Body											
Yoke		10					3	30			
								1410			

Commutator.

Dia. ... Speed ...
Bars ...
Volts p. Bar ...
Brs. p. Arm ...
Size of Brs. ...
Amps p. sq. ...
Brush Loss ...
Watts p. Sq. ...

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.	8	8	8	8	8
Iron Loss	17	17	17	17	17
~~Field~~ Loss Rotor	20	13	7·5	3	1
Arm. &c. I²R	28	18	10	5·5	3
Brush Loss					
	73	56	42·5	33·5	29
Output	1400	1120	840	560	280
Input	1473	1176	883	594	309
Efficiency %	95	95·4	95·2	94·3	90·5

Mag. Cur. 90 Loss Cur. 5
Perm. Stat. Slot 1·93
,, Rot. Slot × 288/360 = 1·96
,, Zig-zag 1·84
2 × 47 × 5·73
1·77 × 540 × 3 = 2860
End 2·45 × 43·5 × 12 = 1275
1350 Amps; Tot 4135
τ = ·067 ; X_a = 1·31
S_1/S_2 = 2·4 ; r_a = ·074 + ·0766

Imp. √·023 + 1·69 = 1·31
Sh. cir. Cur. 1320
Starting Torque 0·36 of full
Max. Torque 2·7 times
Max. H.P. 2·5 times
Slip 1·25 %
Power Factor 0·88

In the case of the 1500 H.P. motor, as a rather heavy overload is required for 3 hours, it will be well to provide room for a little extra copper. We will therefore take K_0 at about 2. The diameter may be varied over fairly wide limits without appreciably altering the cost of the motor. A speed of 6000 feet per minute is a very suitable speed, and gives an economical motor where the frequency is 50 and the motor of large size. 6000 feet per minute gives 2 feet per cycle; that is to say, it gives a pole pitch of 12 inches.

$$D = \frac{6000 \times 12}{\pi \times 246} \times 2{\cdot}54 = 239 \text{ cms.}, \qquad 5 \times 10^5 = \frac{D^2 l \times R_{pm}}{\text{K.V.A.}};$$

$$\therefore\ l = \frac{5 \times 10^5 \times 1360}{239 \times 239 \times 246} = 48{\cdot}5 \text{ cms.}$$

A preliminary calculation based on these figures will show us that 48·5 cms. can be reduced to 47 cms. without unduly saturating the iron of the teeth.

In order to fix upon a suitable number of conductors, we want first to find approximately the magnetic loading $A_g\mathrm{B}$ of the frame. Now, it will be found that in large induction motors the most suitable maximum flux-density in the gap is about 6000 C.G.S. lines per sq. cm. A higher density in the gap up to 9000 C.G.S. lines may be employed in "iron" motors; that is to say, motors with wide teeth and restricted copper space. Such motors call for a large magnetizing current, especially where the air-gap must be fairly great. As the air-gap must be reasonably great (say 2 mms.) on a motor of large diameter, the magnetizing current would be too great if the flux-density were much greater than 6000, and the magnetic pull would be too great if we were to reduce the air-gap.

Taking B_{max} at 6000, provisionally, we get

$$A_g\mathrm{B} = 750 \times 47 \times 6000 = 2{\cdot}11 \times 10^8.$$

From formula (1), page 24,

$$3000 \text{ volts} = 0{\cdot}41 \times \tfrac{250}{60} \times Z_a \times 2{\cdot}11,$$
$$Z_a = 835.$$

The number of poles will be 24, giving a synchronous speed of 250, and a slip of 1·5% will give a full-load speed of 246.

The number of conductors Z_a should be divisible by 24 and again by 3. The nearest number to 835 which satisfies this condition is $864 = 24 \times 12 \times 3$.

If, then, we have 12 slots per pole,* and 3 conductors per slot, the arrangement will be suitable. This gives us 288 slots in the stator. In the choice of the number of slots, regard must be had to the number of segments of stampings which make up a complete circle. There should be, if possible, an even number of slots per segment. As it is desirable, where possible, to have the standard number of segments divisible by 6, we might in this case have 6 segments.

Now, work out the actual $A_g\mathrm{B}$ with 864 conductors

$$A_g\mathrm{B} = \frac{3000 \times 10^8}{0{\cdot}41 \times 4{\cdot}16 \times 864} = 2{\cdot}04 \times 10^8.$$

Take a calculation sheet (see page 448) and fill in the preliminary data.

The drawings of the motor are given in Figs. 406 to 409.

* For the considerations which settle the number of slots per phase per pole see pages 422 and 320.

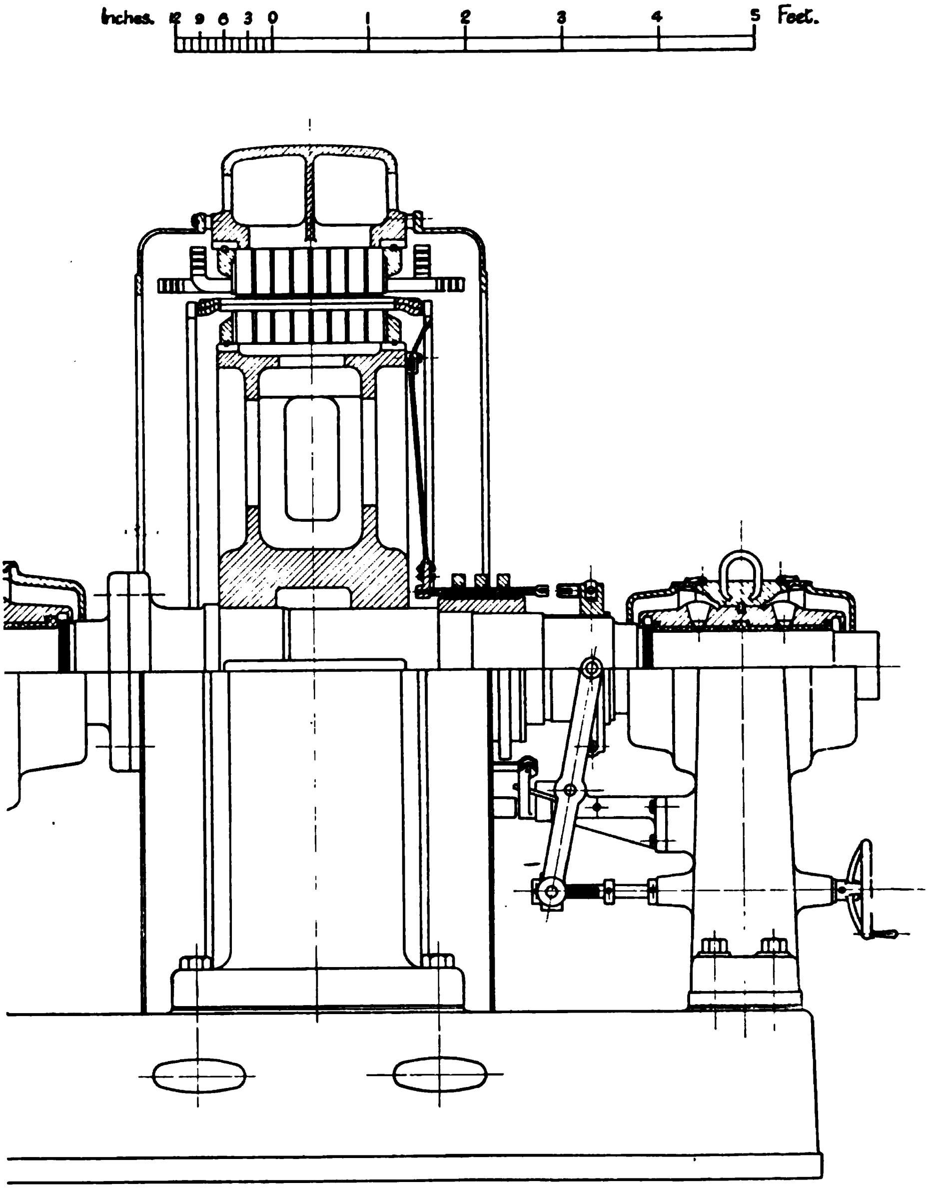
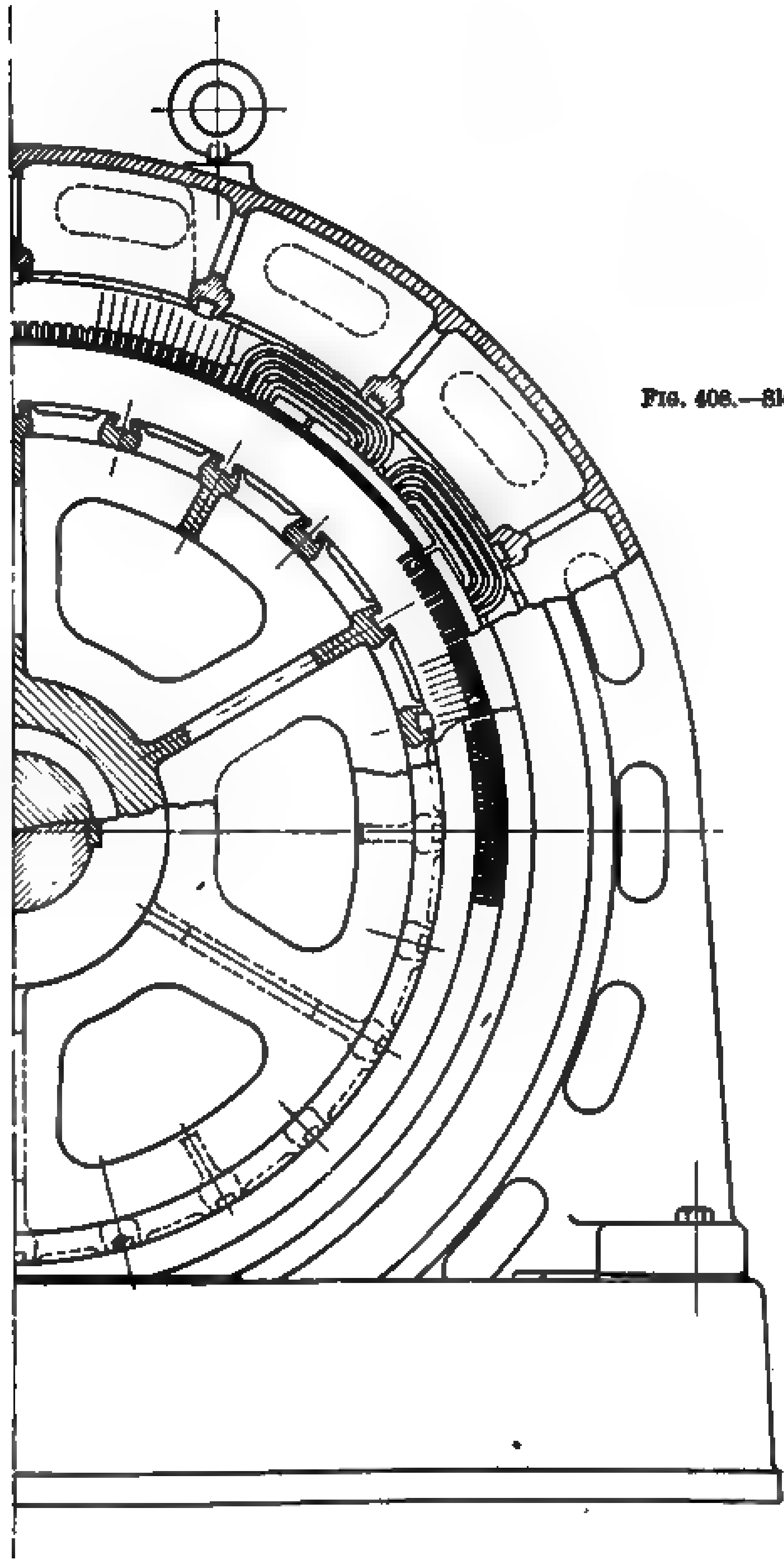


FIG. 407.—Sectional drawings of a 1500 H.P. induction motor.



designed to meet Specification No. 7, p. 488. Scale 1 : 24.

FIG. 408.—Slots of stator and rotor; ½ full size.

FIG. 408a.—Stator slot and insulated conductors; ½ size.

In order to reduce the unbalanced magnetic pull to a minimum, it is a good plan, on a large motor of this kind, to wind two paths in the stator in parallel. This is comparatively easy to do on a 3000 volt motor. Instead of making 3 conductors per slot, we can make 6, and divide each of the phases into two paths in parallel in the manner indicated in Fig. 409. There, phase A is divided into two paths A_1 and A_2 which lie on opposite halves of the frame. It is impossible for the flux on these two opposite halves to be very unequal, as that would necessitate unequal

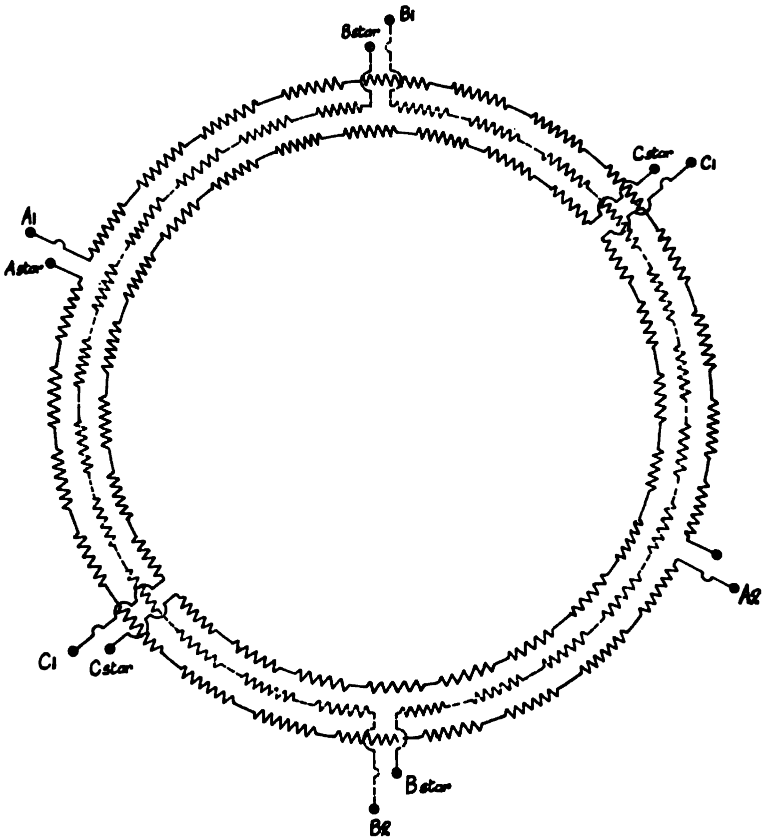


FIG 409.—Diagram of winding of stator, showing two paths in parallel in each phase, to minimise the unbalanced magnetic pull.

back electromotive forces on the two windings A_1 and A_2. The diameters of the frame which divide the phases B and C are set at angles of 120° to the diameter which divides phase A, so as to ensure an equal distribution of flux, notwithstanding a displacement of the rotor in any direction.

We thus get 131 amperes per conductor and 262 amperes per terminal.

The fixing upon the number of slots per phase per pole turns upon such considerations as the following: The more slots we have the less will be the leakage, and the better the cooling of the armature coils. As each armature coil must be fully insulated to earth, a large number of slots per phase per pole would take up

a great deal of room, particularly in high-voltage motors. It thus comes about that in very high-voltage motors the number of slots is kept down to the lowest minimum, consistent with proper cooling and a sufficiently low leakage. It is not advisable to have less than two slots per phase per pole, and if the total current per slot is made very great (say over 1500 amperes), it becomes difficult to conduct through the insulation the heat generated in the coil. In all cases where the proposed arrangement involves a rather large total current per slot, a calculation should be made of the difference of temperature between the inside and the outside of the insulation in the manner indicated in the example given on page 224. Where the voltage is low and the insulation space required not excessive, the number of slots per phase per pole can be made greater and the stator leakage decreased.

In the case under consideration the choice of 4 slots per phase per pole gives a slot pitch of 2·6 cms., quite a reasonably large value for a 3000-volt motor.

In provisionally deciding upon the size of conductor to employ, one may be guided by considerations of current density in the copper, but the final choice of size must depend upon the cooling conditions. A conductor 0·37 sq. cm. in section will carry 131 amperes at a current density of 354 amperes per sq. cm., which, having regard to the over-load conditions, appears to be a reasonable figure. The six conductors, each 0·38 × 1·0 cm., are best arranged as shown in Fig. 408*a*, with a spacer of micanite 0·5 mm. thick between each conductor, held in position with half-lapped tape around each conductor. The allowance to make for this style of insulation is 1·3 mms. per conductor. The whole coil is insulated with paper and mica wrapping, and finally taped to a total thickness of 2·2 mms. After making allowance for wedges and clearances in the slot, it will be found that a slot measuring 1·5 cms. wide by 4·2 cms. deep will be sufficiently large. It is always well to specify plenty of room in the depth, as the cost of the machine is only very little increased by so doing, and the little room in the depth greatly helps in the getting in of a coil that is rather tight in the width. In width the coils should be designed to be a reasonably good fit, so that the heat may be readily conducted from the insulation to the iron, and so that the coil shall not vibrate in the slot.

Before the size of the slot is finally fixed, it is necessary to find the saturation in the teeth. The cross-section of all the teeth is found exactly as described on pages 71 and 322. The figures are given in the calculation sheet on page 448. The axial length of the iron should be adjusted, so that the flux-density in the teeth, one-quarter of a tooth length from the narrowest part, is not more than 17,000. In this case, with a core length of 47 cms. and 7 vents, each 0·8 cm. wide, the flux-density comes out 16,900. The maximum flux-density allowable depends partly on the length of the teeth (for with long teeth the magnetizing ampere-turns become excessive if the flux-density is high), and partly upon the permissible iron loss. With teeth not deeper than 5 cms. and a frequency of 50 cycles, we may take 17,000 lines per sq. cm. as a suitable figure, where the allowable temperature rise is 40° C. For a temperature rise of 45° C. we might allow 18,000. At 25 cycles one will go up to 19,000, except in cases where it is necessary to keep down the magnetizing current to the lowest possible value.

The final arrangement, then, is 288 slots in the stator of the size shown on the calculation sheet, 6 conductors per slot (1728 in all), with two paths in parallel, giving virtually 3 conductors per slot. These figures are entered on the calculation sheet in the manner shown.

The measurement of the mean length of a conductor is best carried out on the drawing of a similar machine. From Fig. 407 we get the length in the slot 47 cms. and the length outside the slot 65 cms., giving a total length of 112 cms. Multiplying 1728 by 112 and dividing by 100, we get 1930 m. for the total length. Multiplying the constant 890 (the weight in kilograms of 1000 metres of conductor 1 sq. cm. in cross-section) by 0·37 sq. cm., we get 330 kilograms for the weight per 1000 metres of conductor, and multiplying by 1·930 we get 640 kilograms as the total weight of the stator copper. This gives us 0·474 kilogram per K.V.A., not an excessive figure considering the operating conditions.

The resistance of the winding we find as on page 143. 0·17 divided by 0·37 gives 0·46 ohm as the resistance of 1000 metres of conductor.

$0{\cdot}46 \times 1{\cdot}93 = 0{\cdot}89$ ohm for all conductors in series.

$0{\cdot}89/4 = 0{\cdot}222$ ohm, two paths in parallel; 0·074 ohm per phase.

To find the difference in temperature between the inside and the outside of the insulation, find the watts per sq. cm. of cooling surface. One metre length of coil will have a loss in it of

$$131 \times 131 \times 0{\cdot}00046 \times 1{\cdot}2 \times 6 = 58 \text{ watts}.$$

As the mean perimeter of a coil is 10·4 cms., the surface may be taken at 1040 sq. cms. The watts per sq. cm. are 0·054. As the thickness of the insulation is 0·25 cm. and the conductivity 0·0012, we have, according to the method given on page 222,

$$\frac{0{\cdot}054 \times 0{\cdot}25}{0{\cdot}0012} = 11{\cdot}7^\circ \text{ C. difference of temperature.}$$

Thus, if the iron of the teeth is 35° C. above the surrounding air, the copper in the slots will be about 47° C. above the air. The allowance of $1/0{\cdot}054 = 18{\cdot}5$ sq. cms. per watt for the exterior of the stator coils, which are subjected to a good draught from the rotor, will ensure the temperature rise of the ends of the coils being well below 40° C. rise (see page 324).

The methods of calculating the cooling surfaces of the stator and the rate at which heat is given off from them are the same as given on page 325 in connection with the 750 K.V.A. generator. The figures are given on the calculation sheet (page 448). The total losses to be carried away from the stator surfaces are, on a liberal computation, 24,300 watts, and with 40° C. rise of the frame we can get rid of 25,700 watts.

The rotor winding. In fixing upon the size and number of rotor conductors, the first step is to decide upon the voltage to be generated in the winding when the slip is equal to the synchronous speed. In large motors we will make this as high as is consistent with safe operation. The higher the voltage the less the current, and the less elaborate the brush gear for collecting it. If the voltage is made too high, it may be dangerous to persons starting the motor, if the brush gear is not perfectly protected; and, moreover, the insulation of the winding is

more difficult to carry out for a high voltage. For large motors of 1000 H.P. or more, rotor voltages of 800 to 1000 are common, and there seems to be no objection to rather higher voltages for very large motors where it is worth while to completely protect the brush gear. In the motor under consideration, if we make 15 slots per pole on the rotor, and use a barrel winding with two bars per slot, the ratio of transformation between stator and rotor will be

$$\frac{288 \times 3}{360 \times 2} = \frac{864}{720}.$$

If all the rotor conductors of one phase were put in series and connected in star, the voltage on the collecting rings at the instant of starting up would be

$$3000 \times \frac{720}{864} = 2500.$$

If the phases were connected in delta, the voltage would be

$$\frac{2500}{1 \cdot 73} = 1445.$$

If the conductors were connected with two paths in parallel and in star, the voltage would be $\frac{2500}{2} = 1250$. The latter seems a suitable voltage for so large a motor, and if this arrangement be adopted, the current per ring at full load will be

$$\frac{1500 \times 746}{1250 \times 1 \cdot 73} = 520 \text{ amperes per ring.}$$

The size of the conductor will then depend upon the amount of slip which we wish to have at full load. If we want to have only $1\frac{1}{4}$ per cent. slip at full load, the resistance of the rotor winding must be adjusted so that the I^2R losses in the rotor are $1\frac{1}{4}$ per cent. of the input to the rotor, or approximately $1\frac{1}{4}$ per cent. of 1120 K.W.; that is to say, 14 K.W. Having fixed the voltage of the rotor winding, and therefore the rotor current at full load, the resistance per phase to give any percentage loss is easily calculated. Allow 1 K.W. for losses on contacts, etc., leaving us 13 K.W. on the winding itself, or 4340 watts per phase.

$$4340 = 520 \times 520 \times r_2,$$

$$r_2 = 0 \cdot 016 \text{ ohm hot, or, say } 0 \cdot 0133 \text{ ohm cold, per phase,}$$

with two paths in parallel, or 0·16 ohm with all conductors in series.

Now the length of one conductor is 95 cms. So the length of 720 conductors is 685 m. If this length is to have a resistance of 0·16 ohm, the resistance for 1000 metres will be 0·234. If we choose a conductor measuring 0·5 cms. × 1·5 cm. and having an area of 0·73 sq. cm., this will have a resistance of $\frac{0 \cdot 17}{0 \cdot 73} = 0 \cdot 234$ ohm per 1000 metres. This will give a resistance per phase with two paths in parallel of 0·0133 ohm (cold), or 0·016 ohm (hot).

It is convenient for many purposes to transform the resistance of the rotor winding by multiplying it by the square of the ratio $\frac{S_1}{S_2}$. We are then able to add it to the stator resistance in calculations of effects occurring in the stator which

depend upon the rotor and stator resistances. This transformed resistance of the rotor we will denote by $r_{2.1}$. In the present case the $\frac{S_1}{S_2}=\frac{864}{360}$, and the square of this ratio is 5·76.

$$r_{2.1}=0·016\times 5·76=0·092 \text{ ohm (hot)},$$

while $$r_1=0·089 \text{ ohm (hot)}.$$

Therefore r_A, the apparent resistance of the motor per phase to alternating current applied to the terminals of the stator is $r_1+r_{2.1}=0·181$ ohm (hot), or 0·1506 ohm (cold).

The rotor winding consists of two conductors per slot. The insulation around each conductor is paper and mica and tape, to a total thickness of 1·8 mm. The whole, with a suitable slot lining and wedge, will go in a slot 0·96 cm. wide by 4·2 cms. deep, as shown in Fig. 408.

The calculation of the flux-density on the teeth is carried out as shown on page 448. The figures are given on the calculation sheet.

The next step is to calculate the magnetizing current. This depends mainly upon the length of the air-gap. It is not advisable to reduce the air-gap of a large motor of this kind much below 2 mm. (see Fig. 401). This air-gap is perfectly satisfactory if provision is made for neutralizing the unbalanced magnetic pull in the manner explained above, and if the design and workmanship on the stator and rotor frames is good.

The contraction ratio when worked out in the manner indicated on page 417 is found to be 1·18. The maximum flux-density in the gap is

$$\frac{2·04\times 10^8}{35200}=5800 \text{ lines per sq. cm.}$$

$$\text{A.T. on the gap}=0·2\times 1·18\times 5800\times\frac{1}{1·257}=1090 \text{ ampere-turns.}$$

The magnetizing current. The calculation of the ampere-turns on the stator and rotor cores and teeth is carried out as indicated on page 448. The figures are given on the calculation sheet. The total ampere-turns per pole are 1410.

To get the magnetizing amperes per phase I_m we adopt the rule given on page 420. For a three-phase, star-connected, full-pitch winding we have

$$\frac{0·437\times I_m Za}{\text{poles}}=\frac{0·437\times I_m\times 864}{24}=1410.$$

$$I_m=90 \text{ amps.}$$

That part of the no-load current which is in phase with the voltage is obtained by dividing the no-load watts by the voltage and by 1·73. The iron loss in this case amounts to 16·8 K.W., and the friction may be taken at 8 K.W., giving a total no-load loss of 24·8 K.W. Thus the watt component of the no-load current amounts to 5 amps. If we take O for the centre of our clock diagram, as in Fig. 410, we can set off ON' to scale to represent 90 amps., and $N'N$ to represent 5 amps.

The next step is to calculate the short-circuit current. As stated above, the most accurate way of arriving at this is to rely upon tests of similar motors built on the same frames or on similar frames. If, however, no such data are available, we may calculate the value of the short-circuit current with a fair approximation by the use of the rules given above for the calculation of the slot leakage, the zigzag

leakage and the end leakage. It should be pointed out, however, that these rules do not take into account the saturation of the iron along the leakage paths, which will probably occur before the current reaches its full short-circuit value. This saturation, however, is only of importance when we wish to know what the actual starting current is. The power factor of the motor, and other particulars of its performance at normal load up to two or three times full load, will be dependent upon the diameter of the circle constructed by taking for the value of the short-circuit current the value that it would be if there were no saturation. The actual starting torque of the motor, however, is dependent upon the amount of saturation which occurs on short circuit. It cannot be determined with any accuracy by calculation. Indeed, two motors built from the same drawings give different starting torques, depending upon slight differences in the amounts of iron in the armatures.

The methods of working out the leakage in stator and rotor and the end leakage have been given on pages 420 to 427. We found that the total leakage per pole for one ampere per phase in the stator winding amounts to 4135 C.G.S. lines. The working flux per pole is found from the formula:

$$\frac{A_g \mathrm{B} \times K_f}{\text{No. of poles}} = \frac{2{\cdot}04 \times 10^8 \times 0{\cdot}66}{24} = 5{\cdot}6 \times 10^6.$$

Therefore the short-circuit current, if there were no resistance, would be

$$\frac{\phi_p}{\phi_l} = \frac{5{\cdot}6 \times 10^6}{4135} = 1350.$$

And we have seen on page 428 that the apparent impedance of the stator is 1·31 per phase, so that actual current on short circuit is

$$I_{sc} = \frac{E_a}{Y} = \frac{1730}{1{\cdot}31} = 1320.$$

We must next calculate the watt component of the short-circuit current. The resistance of the stator per phase is 0·074. The actual resistance of the rotor per phase is 0·016; but, the ratio of transformation being 864 divided by 360, or 2·4, we must multiply 0·016 by $(2{\cdot}4)^2$ to reduce the rotor resistance to its equivalent for a one-to-one ratio. This gives us $r_{2.1} = 0{\cdot}0766$. Therefore $r_1 + r_{2.1} = 0{\cdot}15$ (cold) or 0·18 (hot) per phase. Multiplying by the square of the short-circuit current, and by 3, we get:

$$0{\cdot}18 \times 1320 \times 1320 \times 3 = 940 \text{ watts loss on short-circuit.}$$

Dividing this by 3000 volts and 1·73, we arrive at 180 amperes per phase for the watt component of the short-circuit current. Referring now to Fig. 410, we set off the 180 amperes shown at FS and 1320, represented by $O'S$. The usual practice is to place the point O' at a position midway between the points O and N; the reason for this is that the magnetizing current on short circuit is reduced to about half its normal value, so that the point O really moves half-way towards N. We now know that N and S lie upon the semicircle of the Heyland diagram. The centre of the semicircle is now found by the construction given in Fig. 400 (page 411) and the semicircle drawn through N and S. It will be observed that OS is the short-circuit current calculated on the assumption that there is no saturation

in the path of the leakage line. The semicircle which we have drawn is the locus of the point P of the radius vector OP, which represents the stator current for all normal loads. For greater stator current we should expect some saturation to occur in the leakage path, and the point P will then follow a locus such as that given by the dotted line in Fig. 415, the starting current being somewhat greater than that

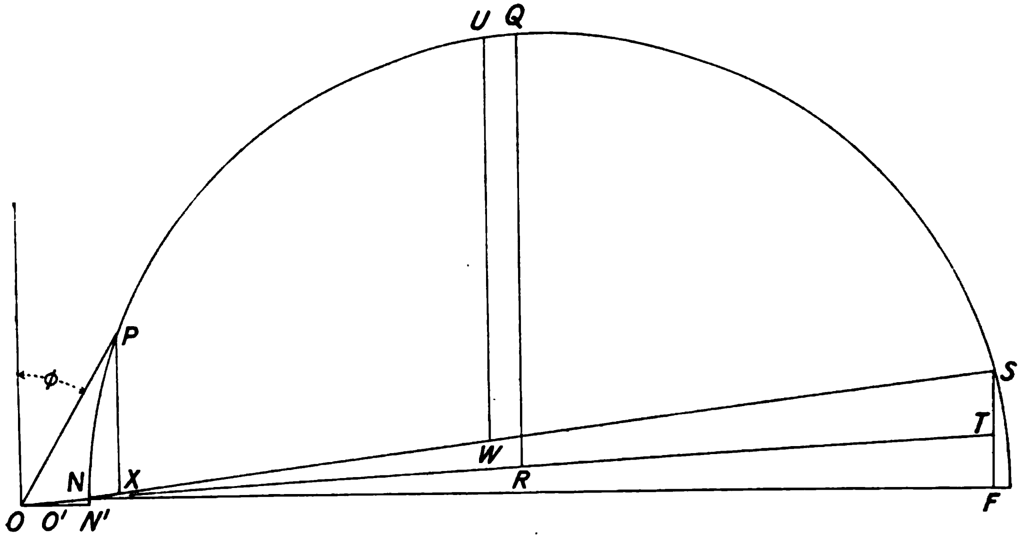


FIG. 410.—Circle diagram of 1500 H.P. motor. Scale 1 mm.=10 amperes per phase.

given by OS. The load line is given by NS. The usual method of obtaining the torque line NT is to divide SF in such a manner that

$$\frac{ST}{TF}=\frac{r_{2.1}}{r_1}.$$

As, however, the short-circuit current in the stator is somewhat greater than that in the rotor, it is better to divide SF so that

$$\frac{ST}{TF}=\frac{r_{2.1}\times NS^2}{r_1\times O'S^2}.$$

This is done in Fig. 410. We then know that any perpendicular such as PX drawn from the semicircle to the load line represents the watt component of the stator current, which when multiplied by the volts and 1·73 gives us the output of the rotor. The value of PX for full load can therefore be found by dividing the full-load output, 1120 K.W., by 3000 and 1·73, giving us 216 amperes per phase. Setting up a perpendicular 21·6 mm., we obtain the full-load stator current OP and the full-load rotor current NP. The power factor, which is the cosine of the angle ϕ, is found to be 0·89. The maximum torque is found by drawing a tangent through Q parallel to the torque line NT. The vertical line QR intercepted by the torque line, when scaled off, gives us 570 amperes per phase, and this at full speed would be equivalent to an output of 2960 K.W. The maximum output is obtained by drawing a tangent through U parallel to NS. The perpendicular UW, when scaled off, gives us a stator current of 530 amperes per phase, which would give a maximum

output of 2700 K.W. The efficiency of the motor is worked out from the separate losses, as indicated on the calculation sheet on page 448.

To obtain the slip we must find the ratio of the rotor losses to the rotor input. The actual current in the rotor is obtained by scaling off NP in Fig. 410 and multiplying by $\frac{S_1}{S_2}$. We thus get

$$217 \times 2{\cdot}4 = 520 \text{ amperes per phase.}$$

Each of the three phases has a resistance of 0·0133 (cold) or 0·016 (hot), so that the I^2R losses at full load are

$$520 \times 520 \times 0{\cdot}016 \times 3 = 13{,}000 \text{ watts.}$$

To this loss we should add about 1 K.W. for brush losses. The input to the rotor will be $1120 + 14 = 1134$ K.W., so that the slip $= \frac{14}{1134} = 0{\cdot}0125$, or 1·25 per cent.

SPECIFICATION No. 8.

350 H.P. INDUCTION MOTOR FOR PUMP DRIVING.

Characteristics of Motor.

120. The Contractor shall supply and erect as described below an induction motor having the following characteristics:

Normal output	350 H.P.
Normal voltage at terminals	2200 volts.
Frequency	50.
Number of phases	3.
Speed	1350 to 1480 R.P.M.
Power factor not less than	*
How connected to load	Direct-connected to centrifugal pump.
Temperature rise after 6 hours full-load run	45° C. by thermometer.
Over load	10 per cent. for 3 hours.
Temperature rise after 3 hours 10 per cent. over load	55° C. by thermometer.
Maximum torque	1·5 times full-load torque.
Starting	By means of rheostat in rotor circuit.
Puncture test	5000 volts on stator. 2500 volts on rotor.

Nature of Load.

121. The motor is for the purpose of driving a centrifugal pump, and for this purpose shall be direct connected to the shaft of the pump situated near the sump well of a coal mine.

Variation of Speed.

122. The normal speed of the pump is 1475 R.P.M., but on certain occasions the speed must be reduced, and may then be between 1350 and 1475 R.P.M. For giving this range of speed, and also for starting the motor, the Contractor shall supply a metallic rheostat fitted with a suitable dial plate having not less than 10 steps.

* The Contractor is to state the power factor at full load and three-quarter load of the motor he proposes to supply.

123. The Contractor shall deliver a separate quotation for this rheostat, giving full particulars of its construction and the temperature rise guaranteed after a 6 hours' run at 300 H.P. at 1350 R.P.M. **Separate Quotation.**

124. The motor will be situated in a dry chamber and supplied with cool dry air. It must be suited in every way for the class of work for which it is intended. **Situation.**

125. Some of the gangways leading to the point where the motor is to be erected are not more than four feet high by six feet wide. The dimensions of the motor and its supports must be such that it can be taken along the said gangways. **Difficulty of Access.**

126. Plan of the mine and of the proposed place of erection can be inspected at the offices of the Purchaser. **Plan.**

127. The bedplate will be supplied by the pump makers. The motor shall be supplied with such feet or other supports as shall be suitable for fitting and bolting to it. Particulars of this bedplate and the height of the running centres will be supplied to the Contractor within three weeks from the giving of the order. At the same time, the Contractor will be given the dimensions of the half coupling, which is also to be supplied by the makers of the pump. **Bedplate.** **Half Coupling.**

128. The Purchaser will undertake the lowering of the motor into the mine and the conveyance along the gangways, provided he is satisfied that the outlines of the motor make it possible; but the Contractor shall carry out the erection and setting to work of the motor. **Carriage through the Mine.**

(See Clauses 99, p. 441; 135 to 137, p. 460.) **Efficiency.**

(See Clause 101, p. 441.) **Tests at Maker's Works.**

129. The tests taken at the maker's works having been carried out satisfactorily, the efficiency of the motor shall be taken as proved.* **Tests on Site.**

* In cases where one Contractor makes himself responsible for the whole plant, pump and motor, it is usual to ask for a guarantee of the combined efficiency of the plant. This is sometimes expressed in terms of so many gallons of water per hour raised a certain height for the consumption of so many electrical units. In asking for guarantees of this sort, care should be taken to specify exactly the points between which the head of water is to be measured. Where tests are to be carried out, it must be clearly stated which party shall provide the measuring tanks and bear the cost of the tests.

Test of Power Factor.

130. After the plant is installed, the Purchaser may call for a test of the power factor of the motor when running under the conditions specified in the guarantee. Such test shall be carried out by taking the ratio of the two readings of a calibrated wattmeter connected first in one phase and then in another. The power factor as worked out from these two readings shall be taken to be the power factor of the motor.

Instruments.

131. The Contractor shall supply all instruments for this purpose. The cost of recalibrating instruments shall be borne by the party requiring the same, unless the instrument shall be proved to be 1 per cent. out of calibration, in which case the cost shall be borne by the Contractor.

Puncture Tests.

(See Clauses 318, p. 611; 234, p. 564.)

DESIGN OF A 350 H.P., 3-PHASE INDUCTION MOTOR TO COMPLY WITH SPECIFICATION NO. 8.

2200 volts; 50 cycles; speed 1350-1475 R.P.M.

As this motor is for the purpose of driving a centrifugal pump, it is not necessary to give it a very great over-load capacity. A maximum torque equal to 1·5 or 1·7 of the full-load torque will be quite sufficient for the purpose. Under the circumstances, an output coefficient, $K_0=2{\cdot}5$ (see page 447) will be ample. This gives us a D^2l constant of 4×10^5. The ratio between diameter and length might be varied over fairly wide limits without appreciably affecting the cost; and it will be impossible, without going very closely into the cost of labour and material in any particular factory, to decide which ratio is best. The speed of the motor being high, the designer will avoid making the radius too great. A diameter of 46 cms. will give a peripheral speed of 36 metres per second, a speed sufficiently high for good ventilation and yet not excessive. With $D=46$, we get $l=38{\cdot}5$, and this ratio will be found to be very economical. It will be noted that the specification requires a motor, the outside diameter of whose frame is less than four feet.

The principal steps in the calculation of the motor will be seen from the calculation form on page 463. Drawings of the motor are given in Figs. 411 and 412.

This is essentially a "copper" machine. Having 4 poles of wide pole pitch, we would, with normal proportions of copper and iron, obtain a motor with a maximum torque some three times full-load torque, a ratio much greater than is needed in this case. Moreover, if the cross-section of copper were kept down the normal rating of the motor would not be so high as it is. In the design given, the copper section has been increased at the expense of the iron section, until the motor has a maximum load not more than 1·7 times its normal load. In making the

Date 8th Aug. 1913 Type ~~GEN.~~ ASYN MOTOR ~~ROTARY~~ 4 Poles .... Elec. Spec. 8

K.V.A. 305; P.F. ·92; Phase 3; Volts 2200; Amps per ter. 80; Cycles 50; R.P.M. 1480; Rotor Amps 320;

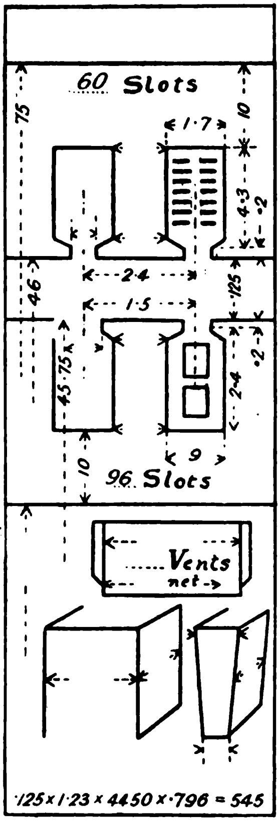
H.P. 350 Amps p. cond. 80 Amps p. br. arm. .... Temp. rise 45°C Regulation .... Overload ....

Customer ....; Order No. ....; Quot. No. ....; Perf. Spec. ....; Fly-wheel effect ....

Frame 75/38·5 Air .... Circum. 144; Gap Area 5550; poss. $A_g B$ ....; poss. $I_a Z_a$ ....; $\frac{I_a Z_a}{\text{Circum.}}$ 466; $\frac{D^2 L \times RPM}{K.V.A.}$ 4×10^5;
$A_g B$ $\cdot 246 \times 10^8$; $I_a Z_a$ 67200

K_a ·415; 2140 Volts = ·415 × 25 × 840 × ·246; Arm. A.T. p. pole ....; Max. Fld. A.T. ....

Armature. ~~Rev.~~	Stat.	
Core		
Dia. Outs.	75	
Dia. Ins.	46	
Gross Length	38·5	
Air Vents	4 - ·75	
Opening Min. ... Mean		
~~Air~~ Velocity	36 m per sec.	
Net Length 35·5 × ·89	31·6	
Depth b. Slots	10	
Section 316 Vol.	64,500	
Flux Density	6700	
Loss ·025 p. cu. cm. Total	1600	
Buried Cu. 1850 Total	2450	4300
Gap Area 5550 Wts	2000	
Vent Area 16,000 Wts	1200	
Outs. Area 14,000 Wts	2100	5300
Teeth		
No of Segs 1 Mn Circ	149	
No of Slots 60 × 1·7 =	102	
K_s	47	
Section Teeth	1490	
Volume Teeth	7000	
Flux Density	16,400	
Loss ·12 p cu cm Total	850	
Weight of Iron	560	
Conductors		
Star ~~or Mesh~~ Throw	1-20, 2-19, 3-18 &c	
Cond. p Slot	14	
Total Conds	840	
Size of Cond. ·42 × ·55	·23 sq. cm.	
Amp. p. sq. cm.	350	
Length in Slots 39		
Length outside 60 Sum		
Total Length 99	840 m	
Wt. of 1,000 205 Total	170 kgs	
Res. p 1,000 ·74 Total	62; ·24 p. ph.	
Watts p. m.	80	
Surface p. m.	1000	
Watts p. Sq.	·08	
·08 × 0·2 / ·0012	13·3°C	



~~Field Stat. or~~ Rotor.			
Dia. ~~Bore~~	45·75		
½ Total Air Gap	·125		
Gap Co-eff. K_g	1·23		
Pole Pitch 36 Pole Arc			
K_f	·7		
Flux per Pole	$4·3 \times 10^6$		
Leakage n.l. ... f.l.			
Area ... Flux density			
Unbalanced Pull			
No. of Seg. 1 Mn. Circ.	132		
No. of Slots 96 × ·9 =	86		
Vents	46		
K_s Section 1410	17,400		
Weight of Iron	270 Kgs.		

	Shunt.	Series.	Comm.
	Rotor Wdg.		
A.T. p Pole n. Load			
A.T. p. Pole f. Load			
Surface per metre	500		
Surface p. Watt	10		
I^2 R.			
I. R.			
Amps.	302		
No. of Turns			
Mean l. Turn			
Total Length			
Resistance	·041	·016 per ph	
Res. per 1,000	0·24	r_2 = ·31	
Size of Cond.	·7 × 1·0		
Conds. per Slot	2		
Total	192		
Length	92 × 192 = 176 m.		
Wt. per 1,000	630		
Total Wt.	107 kgs		
Watts per Sq.	·1		
Star or Mesh	Star		
Paths in parallel			

Magnetization Curve.	Section	Length	... Volts. B.	A.T.p.	A.T.	2140 Volts. B.	A.T.p cm	A.T.	... Volts. B.	A.T.p	A.T.
Core								20			
Stator Teeth	1490	4·4				16,400	45	200			
Rotor Teeth	1410	2·5				17,400	70	175			
Gap	5550	·125				4450		545			
Pole Body								20			
Yoke											
								960			

Commutator.

Dia. .... Speed ....
Bars ....
Volts p. Bar ....
Brs. p. Arm ....
Size of Brs. ....
Amps p. sq. ....
Brush Loss ....
Watts p. Sq. ....

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.		1·9		1·9	
Iron Loss		2·5		2·5	
~~Field~~ Loss in Rotor		4·4		1	
Arm. &c. I^2R		4·7		1·2	
Brush Loss		·3		·2	
		13·8		6·8	
Output		260		130	
Input		274		137	
Efficiency %		95		94·8	

Mag. Cur. 10·4 Loss Cur. 1·15
Perm. Stat. Slot 1·51
,, Rot. Slot × $\frac{60}{96}$ = ·9
,, Zig-zag 2·2
2 × 38·5 × 4·61 = 356
·77 × 356 × 14 = 8820
End 2·45 × 46 × 70 = 7900
256 Amps; Tot. 16,720
r = ·0407; X_a = 5
S_1/S_2 4·38. r_a = ·24 + ·31

Imp. $\sqrt{\cdot 3 + 25}$ = 5·03
Sh. cir. Cur. 253
Starting Torque ·22 of full
Max. Torque 1·7 times
Max. H.P. 1·62 times
Slip 1·65%
Power Factor 0·92

calculation, the method adopted is essentially the same as that given on pages 445 to 459. We have only four poles, and are able to have as many as fifteen slots per pole. We choose a very high current loading per cm. of periphery. Even with a figure as high as 466 amperes per cm., the temperature will not be too high where very efficient ventilation is adopted. We may therefore choose as many as 840 conductors, and in so doing we cut down the value of $A_g\mathsf{B}$, and with it the magnetizing current. Allowing $2\frac{1}{2}$ per cent. for the drop in the stator winding, the back voltage generated by the revolving field may be taken at 2140 volts. We then have the formula

$$2140 = 0{\cdot}415 \times 25 \times 840 \times A_g\mathsf{B},$$

$$A_g\mathsf{B} = 2{\cdot}46 \times 10^8.$$

We have in the stator 60 slots, and 14 conductors per slot. Working the copper at 350 amperes per sq. cm., we adopt a conductor $0{\cdot}42 \times 0{\cdot}5$ cm. Fourteen conductors with insulation (see page 202) will occupy a slot $1{\cdot}7 \times 4{\cdot}3$ cms. The diameter of the mean circle through the slots will be 149; subtracting from this $60 \times 1{\cdot}7 = 102$, we get 47 cms. for the total width of the teeth. The net length of iron is 31·6; the cross-section of all the teeth 1490 sq. cms. Dividing this into $0{\cdot}246 \times 10^8$, we get a flux-density in the teeth of 16,400. From Fig. 29 we find that the loss is 0·12 watt per cu. cm., giving a total loss in the teeth of 850 watts.

The flux per pole is obtained from the formula (1) on p. 326.

$$\frac{0{\cdot}246 \times 10^8 \times 0{\cdot}7}{4} = 4{\cdot}3 \times 10^6 \text{ C.G.S. lines per pole.}$$

Allowing 10 cms. depth behind the slots, we get a cross-section of core of 316 sq. cms.:

$$\frac{4{\cdot}3 \times 10^6}{2 \times 316} = 6700 \text{ C.G.S. lines per sq. cm. in the core.}$$

This gives a loss of 0·025 watt per cu. cm., and a total iron loss of 2450 watts. The buried copper loss amounts to 1850, giving a total of 4300 watts, to be dissipated by the iron surfaces of the core. It will be seen that with 45° C. temperature rise, the iron surfaces of the core can dissipate 5300 watts. We find that there are 80 watts lost per metre length of armature coil. As the cooling surface per metre is 1000 sq. cms., we have 0·08 watt per sq. cm. As the thickness of insulation is 0·02, and the heat conductivity 0·0012 (see page 221), we may expect a difference of temperature of 13·3° C. between the copper and the iron.

Flux-density in the air-gap. This is obtained by dividing the $A_g\mathsf{B}$ by the gap area:

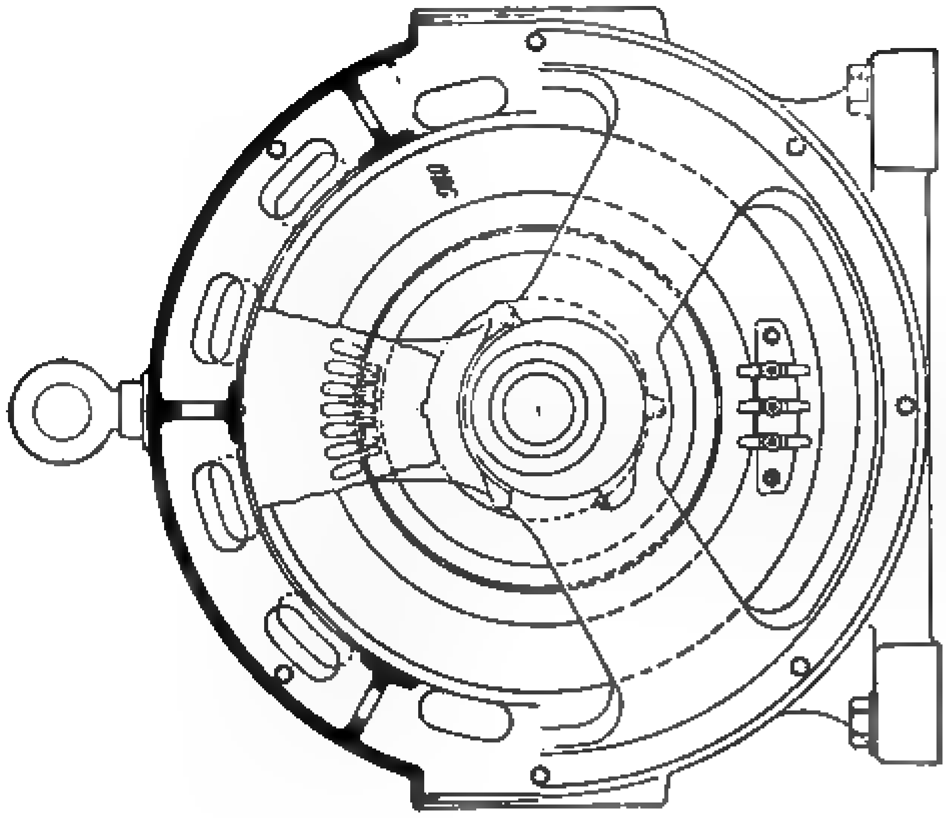
$$\frac{0{\cdot}246 \times 10^8}{5550} = 4450 = \mathsf{B}_g.$$

The gap coefficient, worked out from Figs. 36 and 37, is 1·23; so that the ampere-turns on the gap are:

$$0{\cdot}125 \times 1{\cdot}23 \times 4450 \times 0{\cdot}796 = 545 \text{ ampere-turns.}$$

The flux-density in the rotor teeth, as worked out on the right-hand side of the form, is 17,400.

Magnetizing current. We now proceed to calculate the magnetizing current. We see from the sheet that at 2140 volts the ampere-turns per pole required are 960.



FIGS. 411 and 412.—A four-pole, 50-cycle induction motor capable of yielding a maximum torque of 1900 lbs. at a foot radius, and complying with Specification No. 8 (p. 460). Scale $\frac{1}{5}$th full size.

The design is by the Allgemeine Elektricitäts-Gesellschaft, but the rating is adapted to fit the special running conditions.

As there are 210 conductors per pole, the magnetizing current is obtained from the formula:

$$I_m = \frac{960}{0\cdot437 \times 210} = 10\cdot4 \text{ amperes.}$$

The core-loss current equals 2450. To find the current in phase with the voltage at no load, we add the iron loss 2450 watts to the friction and windage losses, which may be estimated at 1900, and divide the sum by $2200 \times 1\cdot73$. This gives us 1·15 amperes.

Rotor conductors. The output of the rotor is 350 H.P., or 261 K.W. A standstill voltage of 500 will give us about 300 amperes per ring. This is a suitable current for a motor of this size. To generate a standstill voltage of 500, we must have a transformation ratio of about 4·4. If we choose 96 slots and 2 conductors per slot, making 192 conductors in all, the transformation ratio will be

$$\frac{840}{192} = 4\cdot38.$$

We therefore decide upon 192 conductors. They will form a barrel winding, as shown in Fig. 411.

A current density of 460 amperes per sq. cm. is not too high for the rotor copper, so that a conductor 0·7 cm. will be large enough. Two conductors $0\cdot7 \times 1$, with their necessary insulation (see page 202), will occupy a slot $0\cdot9 \times 2\cdot4$ cms. The total length of all the rotor conductors works out at 176 metres, and they have a total resistance of 0·041 ohm; so that the resistance per phase is 0·0137 ohm cold, or 0·016 hot. Multiplying this by the square of the transformation ratio $\left(\frac{S_1}{S_2}\right)^2 = 4\cdot38^2$, gives us $r_{2.1} = 0\cdot31$.

The method of working out the permeance of the stator and rotor slots and the zigzag leakage is the same as that described on pages 422 to 427. The permeance of the stator slots is 1·51; for the rotor slots it is 1·44, which, multiplied by the ratio $\frac{60}{96}$, gives 0·9. The zigzag permeance works out at 2·2; so that the total permeance for 1 cm. axial length is 4·61. This, multiplied by 38·5 and by 2, gives us 356 for the permeance per pole. For one ampere passing in the stator winding we have:

$$1\cdot257 \times 1\cdot41 \times 14 \text{ wires per slot} \times 356 = 8820 \text{ lines per pole for 1 ampere.}$$

Next, consider the leakage around the end winding. Referring to Table XVIII. page 427, the value for K_L for concentric stator winding and barrel rotor winding is 2·45. The pitch of the pole is 36 cms., and the value of a_v is 10 cms., giving

$$l + a_v = 46.$$

There are 5 slots per phase per pole, each carrying 14 wires; so that for one ampere passing in the stator the virtual ampere-turns are 70. Thus we get the leakage around the end windings equal to 7900, giving a total of 16,720 leakage lines per pole for one ampere in the stator. Now, the working flux to generate full voltage is $4\cdot3 \times 10^6$ lines, so that it will take 256 amperes in the stator to produce enough leakage to generate a back E.M.F. equal to the E.M.F. supplied. As the voltage per phase is 1270, this divided by 256 gives us an apparent reactance of 5 ohms

per phase. In order to find more exactly the short-circuit current, it is necessary to take into account the value of the stator and rotor resistances ; these are worked out on the calculation sheet. r_A, the sum of the stator resistance, and the rotor resistance referred to the stator, is $0{\cdot}24 + 0{\cdot}31 = 0{\cdot}55$. The impedance then works out at 5·03 ohms, giving a short-circuit current of 253 amperes. The actual test on the frame of this motor gave readings between 230 and 270 short-circuit amperes per phase, depending on the position of the rotor slots relative to the stator slots. From these data we draw the circle diagram as described on page 414. From it, we find that the starting torque with no resistance inserted is 0·22 of the full-load torque; the maximum torque is 1·7 times full-load torque, and the maximum horse-power 1·62 times the normal horse-power.

Slip. The slip is found by taking the ratio of the I^2R losses in the rotor at full load, equal 4·4 K.W., to the total rotor input, equal 266 K.W.

$$\frac{4{\cdot}4}{266} \times 100 = 1{\cdot}65 \text{ per cent.}$$

SMALL MOTORS.

In drawing a specification for a small motor, one should aim at making it as simple as possible, confining oneself to those matters which are important from the purchaser's point of view, and leaving to the manufacturer as free a hand as possible in the design, so that he may be able to put forward one of his standard machines. A standard motor will probably be much cheaper and more quickly delivered than a special motor built to comply with a specification which too rigidly prescribes its characteristics. It is particularly important that the specification should be confined to performance, and not dictate the methods of manufacture by which that performance can be obtained. It may be well in some cases to call for a motor having a certain power factor, but it is better to leave the efficiency to the manufacturer and see what figures can be guaranteed. The following form may be taken as a guide in the case of a small motor.

SPECIFICATION NO. 9.

35 H.P. INDUCTION MOTOR.

Purposes of the Motor.

132. There shall be supplied a three-phase induction motor for the purpose of driving a line of shafting in a carpenter's shop.

Type of Rotor.

133. The rotor shall be of the squirrel-cage type.

Characteristics.

134. The motor shall have the following characteristics:

Normal output	35 H.P.
Normal voltage at terminals	500 volts.
Frequency	50 cycles.
Number of phases	3.
Speed	960 revs. per minute.
Power factor	Not less than 0·8.
How connected to load	Belted.
Size of steel pulley to be supplied	24″ dia. × 12″ face.
Temperature rise after 2 hours' full-load run	45° C. by thermometer.
Over load	20 per cent. for 15 minutes.
Maximum torque	2·5 times full-load torque.
Puncture test	1500 volts alternating applied for 1 minute between windings and frame.

Pulley and Slide Rails.

135. The motor shall be provided with a pulley of the size above specified, and be mounted on slide rails with belt-tightening screws.

Extent of Work.

136. The contract includes the delivery of the motor at the purchaser's works in   , together with certain switch gear and starting gear, but does not include erection or starting-up.

137. The contractor shall state the amounts of the following losses in the motor which he supplies: **Efficiency.**

1. Bearing friction and windage losses. (At no load.)
2. Iron losses at no load, when run on 500 volts 50 cycles.
3. Armature and rotor copper losses at full load, allowing for temperature rise.

The contractor shall state what calculated efficiency he guarantees on the basis of these separate losses, as well as the actual efficiency of the motor at full, three-quarter and half load.

138. The motor shall be run for one hour at full load at the contractor's works in the presence of the purchaser's engineer. On this test the power factor shall be measured both by power-factor meter and by the two-wattmeter method. Measurements shall also be taken of the power taken to run the motor at no load, and of the power absorbed when the motor is locked and taking full-load current on short circuit. Measurements shall be made of the maximum torque. When the motor is still warm after these tests, a pressure of 1500 volts alternating, 50 cycles, shall be applied between the stator copper and frame for one minute. **Tests.**

138*a*. If the motor is found to fulfil the guarantees, so far as can be ascertained by these tests, it shall be accepted without further tests. In view of the difficulty of measuring the actual efficiency in a commercial test, the calculated efficiency shall be taken as the criterion, unless there is very positive evidence that the motor falls below its guarantees in actual efficiency. **Acceptance.** **Efficiency.**

138*b*. If during the first six months after delivery any defects in construction or performance become manifest, the same shall be immediately rectified by the contractor at his expense. Any time elapsing between the reporting of the defects and the remedying of the same shall not be counted in the six months' period of maintenance. **Period of Maintenance.**

DESIGN OF A 35 H.P. INDUCTION MOTOR.

500 volts ; 3-phase ; 50 cycles ; 980 R.P.M.

A motor of this kind would form one of a manufacturer's standard line of motors. Its rating would have been determined by actual trial, so that in practice one would not work out its diameter and length from first principles, but take a motor from the list whose rating is known. Nevertheless, it is of interest to apply the rules which we have given above to see how far they are of use in predetermining the performance of the motor from the dimensions.

In the first place, it will be found that for these small motors the output coefficient, K_0, is smaller than in large motors. The output coefficient of an induction motor will depend upon the point of the circle diagram for the frame which is taken as the full-load point. If a motor with great over-load capacity is wanted, the full-load point (P, in Fig. 400) must be taken nearer the origin than where a motor of smaller over- oad capacity is wanted, and the rating of the frame must be correspondingly decreased. In this case the specification calls for a motor which will yield 2·5 times full-load torque. Referring to Table XIX. page 447, we may take K_0 at something below 1·55. The D^2l constant comes out at $6{\cdot}5 \times 10^5$. Take a diameter 40 cms., and an axial length 14 cms. If a fairly good power factor is desired on these small motors, it is well to keep the diameter great as compared with the length, because on a great circumference one has more room for increasing the number of slots, and the number of turns per pole can be increased, and thus the magnetizing current will be decreased. The pole pitch is 20·8 cms., about 50 % greater than the axial length, and the ratio of active length of stator conductor to total length is only $\frac{14}{48}$. If the power factor were less important one could increase the axial length of iron and reduce the diameter and reduce the weight of copper in the stator.

The calculation sheet on page 471 gives full details of the steps in the working out of the losses and cooling conditions. The motor is illustrated in Figs. 413 and 414. The circle diagram is plotted to scale in Fig. 415.

The large diameter gives us room for 72 slots of the dimensions given on the calculation sheet. This is a convenient number for a standard motor, as it enables the frame to be wound for either 6 poles or 8 poles.

The stator winding will be a " mush " winding of the type illustrated in Fig. 138. The number of conductors is determined by the magnetic loading of the frame. With a standard punching and a given axial length, there is a maximum magnetic loading beyond which we cannot go without saturating the iron too highly. We may take a flux-density of 18,000 in the teeth as a suitable figure for these small motors of 50 cycles. At 25 cycles we might go to a flux-density in the teeth of 19,500, not merely because the iron loss is lower at 25 cycles, but because we have fewer poles and consequently a larger number of ampere-turns per pole, so that we can afford to have a higher magnetic reluctance. With 18,000 lines per sq. cm. and 600 sq. cms. in all the teeth, we have an $A_g\mathrm{B}$ of $0{\cdot}106 \times 10^8$. Before we can settle on the number of conductors, we must decide whether we will connect the

Date 6th Jan. 1914 Type ~~GEN.~~ ASYN MOTOR ~~ROTARY~~ 6 Poles Elec Spec. 9

K V A. 34 ; P F. .88 ; Phase 3 ; Volts 500 ; Amps per ter. 39 ; Cycles 50 ; R.P.M. 980 ; Rotor Amps ;

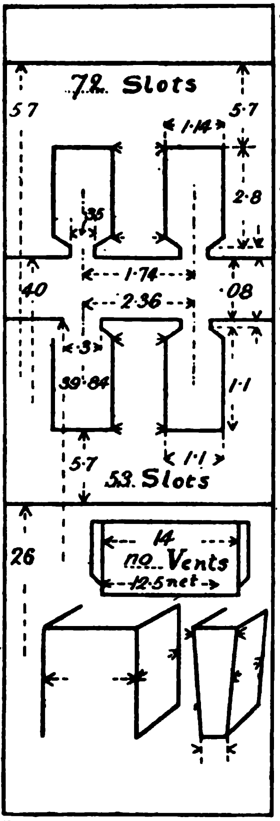
H.P. 35 Amps p. cond. 22·6 Amps p. br. arm Temp. rise 45°C. Regulation Overload 20 for ½ hr.

Customer ; Order No. ; Quot. No. ; Perf. Spec. ; Fly-wheel effect

Frame 57/14 Air Circum. 125 ; Gap Area 1750 ; poss. A_g B ; A_g B .106 × 10⁸ ; poss. $I_a Z_a$; $I_a Z_a$ 26,100 ; $\frac{I_a Z_a}{Circum.}$ 210 ; $\frac{D^2 L \times RPM}{K.V.A.}$ 6·5 × 10⁵ ;

K_e ·24 ; 490 Volts = ·24 × 16·6 × 1152 × .106 ; Arm. A.T. p. pole 2200 Max. Fld. A.T.

Armature. ~~Rev.~~ Stat.			
Core	Dia. Outs	57	
	Dia. Ins.	40	
	Gross Length	14	
	Air Vents none		
	Opening Min. Mean		
	Air Velocity	20m. per sec.	
	Net Length 14 × ·89	12·5	
	Depth b. Slots	5·7	
	Section 71·5 Vol.	11,500	
	Flux Density	8300	
	Loss ·04 p.cu.cm. Total	460	
	Buried Cu. 370 Total	716	1086
	Gap Area 1750 ; Wts	480	
	Vent Area ; Wts		
	Outs. Area 6400 ; Wts	960	1840
Teeth	No of Segs 1 Mn.Circ.	130	
	No of Slots 72 × 1·14 =	82	
	K_e	480	
	Section Teeth	600	
	Volume Teeth	1700	
	Flux Density	18,000	
	Loss ·15 p.cu cm. Total	256	
	Weight of Iron	102 kg	
Conductors	~~Star or~~ Mesh Throw	1 and 12	
	Cond. p. Slot 32 + 2	16	
	Total Conds 2304 ÷ 2	1152	
	Size of Cond. ×	·062 sq. cm.	
	Amp. p. sq. cm.	364	
	Length in Slots 14		
	Length outside 34 Sum		
	Total Length 48	553 m.	
	Wt. of 1,000 55 Total	30·5 kgs	
	Res. p. 1,000 2·74 Total	1·51 cold	1·77 hot
	Watts p. metre	25·4	
	Surface p. metre	700	
	Watts p. Sq. cm. ·037 × 0·1 ·0012	·037	



Field ~~Stat. or~~ Rotor.		
Dia. ~~Bore~~	39·84	
½ Total Air Gap	·08	
Gap Co-eff. K_g	1·22	
Pole Pitch 20·8 Pole Arc		
K_f	·66	
Flux per Pole	1·19 × 10⁶	
Leakage n.l. f.l.		
Area 77 Flux density		
Unbalanced Pull		
No. of Seg. 1 Mn.Circ.	122	
No. of Slots 53 × 1·1 =	58	
Vents none 13·8	64	
K_e Section 86		
Weight of Iron 65 kgs.		

	~~Shunt.~~ Bars	~~Series.~~ Rings	~~Comm.~~
A.T. p Pole n.Load			
A.T. p. Pole f.Load			
Surface			
Surface p. Watt			
I^2. R.	270	210	
I. R.			
Amps.	380 per bar; 1100 in ring.		
No. of Turns			
Mean ~~1. Turn~~ l	18 cms.		
Total Length	950 cms.	240 cms.	
Resistance	·00185	·00017	
Res. per 1,000	·17	·06	
Size of Cond.	1 sq. cm.	3 sq. cms.	
Conds. per Slot	1		
Total Length			
Wt. per 1,000			
Total Wt.		12·0 kilograms	
Watts per Sq.			
~~Star or Mesh~~ Squirrel cage			
Paths in parallel			

Magnetization Curve.	Section.	Length	...Volts. B.	A.T.p.	A.T.	500 Volts. B.	A.T.p.cm	A.T.	...Volts. B.	A.T.p.	A.T.
Core	71·5					8300	2	12			
Stator Teeth	600	2·8				18000	100	280			
Rotor Teeth	840	1·1				12,500	78	86			
Gap	1750	·08				6170		480			
Pole Body											
Yoke								12			
								870			

Commutator.	
Dia. Speed	
Bars	
Volts p. Bar	
Brs. p. Arm	
Size of Brs.	
Amps p. sq.	
Brush Loss	
Watts p. Sq.	

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.		·50			
Iron Loss		·72			
~~Field Loss~~ Rotor		·48			
Arm. &c. I^2R		·90			
Brush Loss					
		2·67			
Output		26			
Input		28·6			
Efficiency %		91			

Mag. Cur. 17·8 Loss Cur. ·8
Perm. Stat. Slot = 1·8
" Rot. Slot × = 1·24
" Zig-zag 4·0
2 × 14 × 7·04
1·77 × 197 × 16 = 5600
End 2·6 × 29 × 64 = 4820
197 Amps; Tot. 10,420
τ = ·09 ; X_a = 4·38
S_1/S_2 ; r_a = ·59 + ·52

Imp. $\sqrt{1\cdot 23 + 19\cdot 2}$ = 4·5
Sh. cir. Cur. 111 in Δ 192 in ⅄
Starting Torque
Max. Torque 2·6 times
Max. H.P. 2·4 times
Slip 2%
Power Factor ·83

stator in star or in mesh. If the motor were to be started on a resistance in the rotor circuit, we would prefer a star-wound stator, because the number of wires would be fewer and the copper factor better. This motor, however, is to be of the squirrel-cage type, and is to be started by connecting it across the mains first in star and then in mesh. The stator must therefore be designed to work at full voltage when mesh connected. If we take the coefficient K_e as 0·415 for a 3-phase star-connected stator, it will be 0·24 for a mesh-connected stator. Thus we arrive at our voltage formula (see page 25):

$$490 = 0{\cdot}24 \times 1{\cdot}66 \times Z_a \times 0{\cdot}106,$$
$$Z_a = 1152,$$
$$1152 \div 72 = 16 \text{ conductors per slot.}$$

For ease in winding, we choose round wire double-cotton covered, 0·031 sq. cm. in cross-section. There are thus 32 wires per slot, two in parallel. The current per double conductor is 22·6 amperes, so that the current density is 364 amperes per sq. cm. As the coils are huddled closely together in the manner shown in Fig. 138, this current density will be found quite high enough. The size of slot is given on the sheet. The mouth of the slot is made 0·35 cm. in order to facilitate the introduction of the wires.

Cooling conditions. The iron loss as worked out on the calculation sheet amounts to 716 watts, and the buried copper loss 370 watts, so that the total watts to be dissipated by the iron surfaces of the stator are 1086. From the rules given in Chapter X., we see that for 45° C. rise we can get rid of 1340 watts, so we have not much in hand. The rotor should be provided with a fan at each end to blow air over the stator winding. It is a good plan to make passages behind the stator frame as shown in Fig. 413, so that the air can get away readily and at the same time cool the cast-iron of the frame.

The air-gap. On a small motor like this the air-gap may be made just as small as is consistent with ensuring mechanical clearance under practical working conditions. A clearance of 0·08 cm. will be found to be sufficient. With very good workmanship and ball-bearings it could be reduced still further, but it will be seen that the ampere-turns on the gap 0·08 cm. long only amount to about one-half of the total ampere-turns, so that it is not worth while to reduce the length. Moreover, a reduction of the length will increase the zigzag leakage, which is already large on those motors with a big ratio of tooth pitch to air-gap.

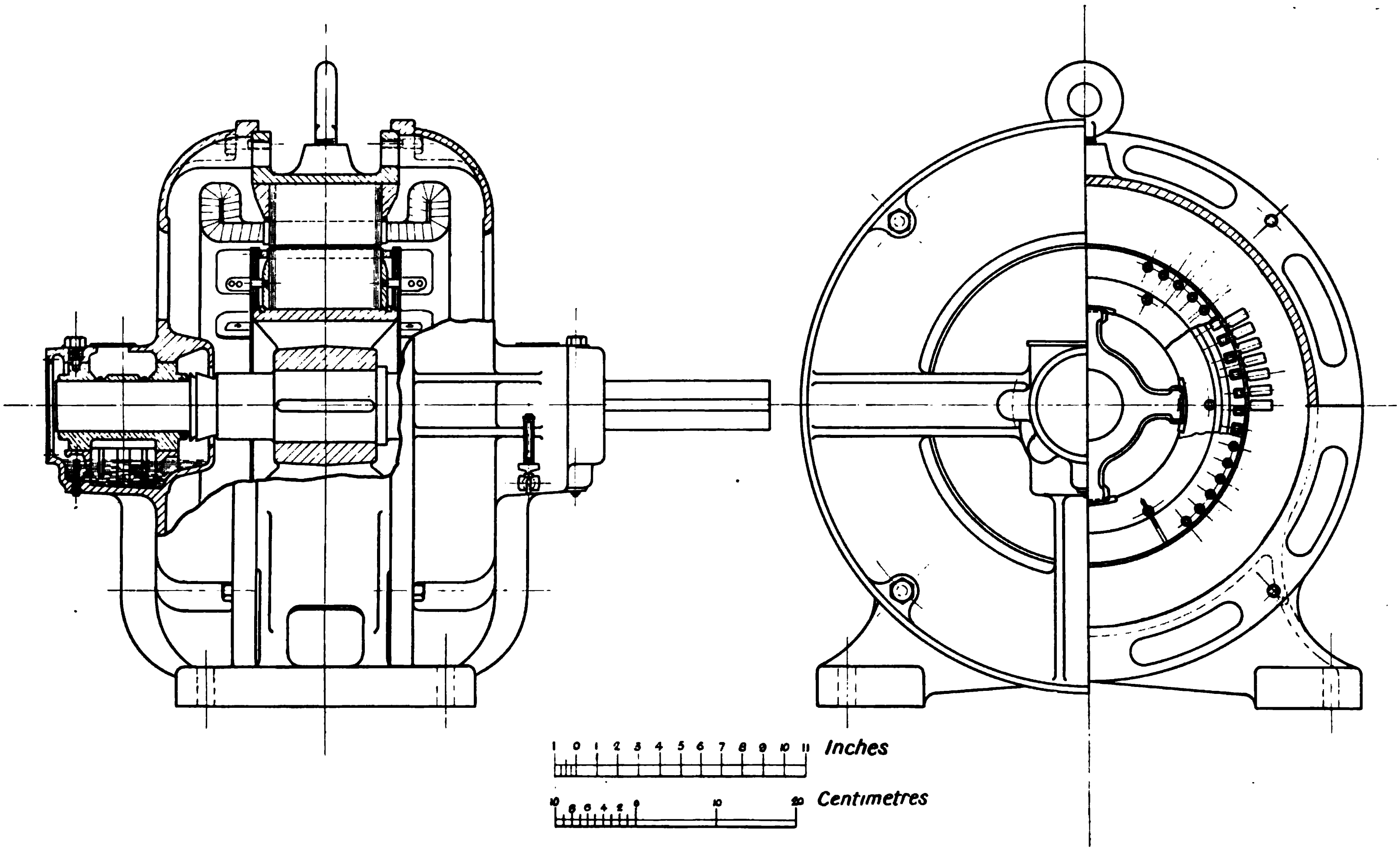
Magnetizing current. It will be seen that, owing to the short air-gap and wide opening at the mouths of the slots, the gap coefficient K_g is fairly great, 1·22. If the ampere-turns in the gap are

$$6170 \times 0{\cdot}08 \times 1{\cdot}22 \times 0{\cdot}796 = 480,$$

the total ampere-turns per pole are 870.

In working out the magnetizing current we must remember that the stator is mesh connected. The number of conductors per pole is 192, so in the mesh

$$0{\cdot}437 I_{mm} \times 192 = 870,$$
$$I_{mm} = 10{\cdot}3,$$
$$I_m \text{ in the star} = 10{\cdot}3 \times 1{\cdot}73 = 17{\cdot}8.$$



FIGS. 413 and 414.—A 35 H.P., 3-phase, squirrel-cage induction motor, 500 volts, 50 cycles, 980 R.P.M., designed to meet Specification No. 9.

Squirrel cage. A fairly simple way of estimating the resistance $r_{2.1}$ of the squirrel cage as measured by its effect on the stator current on short-circuit is to make a rough estimate of the total losses in the bars and end rings with some particular current (say full-load current) flowing in them. First find the total ampere-wires in the stator with full-load working current. This is 20,200. Divide by 53 bars in rotor, and we get 380 amperes (virtual) per bar. Assuming a distribution on the rotor similar to that in the stator, we arrive at 1100 amperes (virtual) in the ring. Now work out the total resistance of all the bars, allowing something for joints. This is 0·0016 (cold) or 0·00185 (hot). The total loss in the bars will be

$$380 \times 380 \times 0{\cdot}00185 = 270 \text{ watts.}$$

Now, the resistance of both rings measured right around each ring $= 0{\cdot}00017$ (hot), so the loss in the rings is

$$1100 \times 1100 \times 0{\cdot}00017 = 210 \text{ watts.}$$

Thus, the total loss in the rotor winding at full load is 480 watts.

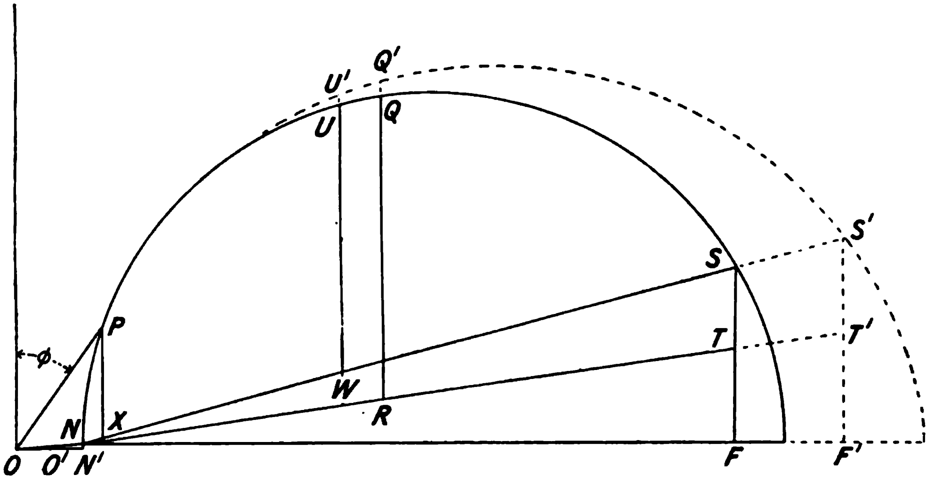


FIG. 415.—Circle diagram of 35 H.P. induction motor, particulars of which are given on page 468. Scale 1 mm. = 2 amperes per phase.

Now, the resistance of the stator winding is 1·77 ohms, so the working current of 17·5 amperes will cause a loss of 545 watts. Therefore

$$\frac{r_{2.1}}{r_1} = \frac{480}{545}.$$

If the resistance of the limb of the stator be taken at $1{\cdot}77 \div 3 = 0{\cdot}59$, then $r_{2.1} = 0{\cdot}52$ ohm.

Slip. Taking the I^2R loss in the rotor at 480 watts and the output of the rotor at 25,400 watts, the input to rotor is 25,880 watts. The slip therefore will be

$$\frac{480}{25{,}880}, \text{ or about } 2\ \%.$$

Leakage factor. The method of working out the stator and rotor leakage is exactly similar to that described on pages 420 to 428. The figures are given on page 471. The short-circuit current is 192 amperes per phase. The power factor is found from Fig. 415.

CHAPTER XVIII.

CONTINUOUS-CURRENT GENERATORS.

CONTINUOUS-CURRENT generators of the hetero-polar type necessarily generate alternating current within the armature itself, the current being transformed to a continuous stream by means of the commutator. The rules, therefore, that have been given for the calculation of the magnetic circuit, the iron losses and the copper losses of alternating-current generators are applicable to continuous-current generators.

There are, however, certain matters which are peculiar to the C.C. generator, the most important of which is the bringing about of good commutation. We propose to consider some of these in this chapter. It is not within our province to describe the various types of winding which are used on these machines: they are fully dealt with in many excellent text-books. We shall assume that the student is familiar with C.C. windings; we shall only deal with the various types of winding in so far as is necessary to determine the choice between one type and another.

Leaving out of account open-circuit windings, which are only used in very special cases, the winding on an ordinary C.C. generator consists of a mesh-connected multi-phase winding, the number of phases being equal to the number of commutator bars per pole. By making the phases very numerous, and by making a connection between each phase and a commutator bar, we are able to generate a very uniform voltage. This voltage is made up of the sum of the E.M.F.'s generated in all the phases, except the one or two which are short circuited by the brushes, and are undergoing commutation. Two advantages accrue from the increase in the number of phases: in the first place, the variation of voltage caused by the cutting-in and cutting-out of a new phase is reduced; in the second place, the self-induction of each coil under commutation is reduced. If we can increase the number of phases until each consists of only one turn, we have approached the ideal condition. In some cases it is even advisable to go further than this, and make a connection to a commutator bar after each half-turn. So important is it to keep down the value of the E.M.F. necessary to reverse the current in the coil under commutation, that in large machines a number of circuits are arranged in parallel, there being as many poles provided on the field-magnets as there are circuits in parallel: thus only a fraction of the current to be delivered is dealt with by one pair of poles.

Commutation. The conditions which are to be met in order to secure good commutation may be shortly stated as follows: We shall speak of one section of the winding, the ends of which are connected to successive commutator bars and which really constitute one phase of our multi-phase generator, as an *armature coil*. In large machines each coil consists of only one turn. In Fig. 420 are shown diagrammatically a number of coils connected to commutator bars: let us fix our

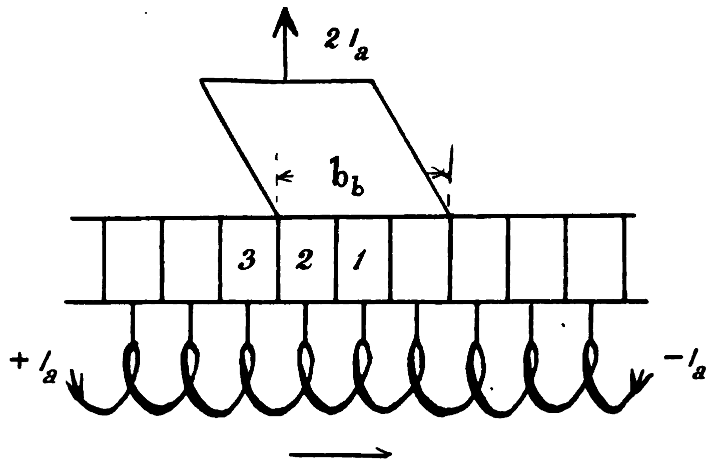


FIG. 420.—Commutator bars passing under brush.

attention upon one of these connected between bars 2-3 as the armature moves forward in the direction indicated by the arrow. We see that the current through the coil is going from left to right before the coil reaches the brush, and going from right to left after the coil passes the brush. The interval of time during which the coil is short circuited by the brush is $t_c = \frac{b_b}{v_c}$, where b_b is the breadth of the brush in centimetres, and v_c is the velocity of the commutator surface in centimetres

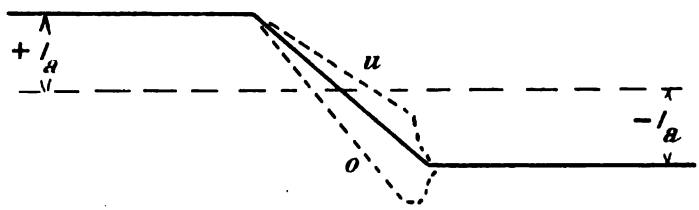


FIG. 421.

per second. During this interval of time the current must be completely reversed; and if we are to have no tendency to spark when the bar 2 leaves the toe of the brush, the current in the coil 2-3 must have grown to be exactly equal to the current in coil 1-2. This reversal of the current is most satisfactorily brought about by introducing an E.M.F. (called here the commutating E.M.F.) into the coil during the interval of short circuit. If this commutating E.M.F. is too small, it will fail to build up the reverse current to the value of the normal armature current before bar 2

leaves the toe of the brush, and we must rely upon the resistance of the carbon brush to force the commutation just at the last instant (see curve u in Fig. 421); while, on the other hand, if it is too great, it will build up the reverse current to a value higher than the normal armature current and cause sparking from "over-commutation," if the resistance of the brush is not sufficient to force the current to the right value at the last instant. It will be seen that the ideal commutating

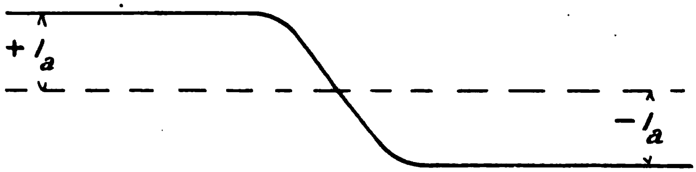


FIG. 422.

E.M.F. is one which will be zero at no load, and which will increase as the load comes on, so as to be equal at all times to $\frac{2I_a}{t_c} \times L$, where L is the coefficient of self-induction of the coil under commutation and $\frac{2I_a}{t_c}$ is the rate of change of the current which is necessary to produce an exact reversal in the interval of time t_c. As L is fairly uniform for a wide range of I_a, it is desirable in general that the commutating E.M.F. shall increase in proportion to the load. This commutating E.M.F. will be generated in the coil under commutation if during the commutation interval the coil is moving under a commutating pole, as illustrated in Fig. 429.

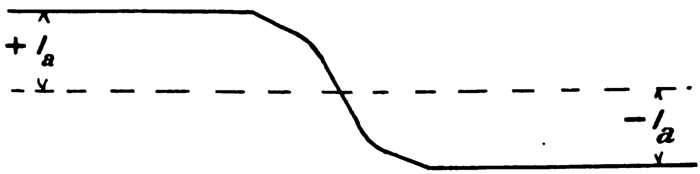


FIG. 423.

If the commutating E.M.F. is nearly constant during the commutating interval, the rate of change of the current will be almost constant, and the current in the coil will change from positive to negative in the manner shown by the full line in Fig. 421. If we bring about commutation according to this straight line law, the commutating E.M.F. will be a minimum, but it is safer to make the rate of change greater in the middle of the commutating interval and smaller at the end of the interval. The resistance of the brush considerably affects the shape of the curve and helps the current in the coil to reach the right value before the brush leaves the bar. Thus, if the commutating E.M.F. is too small, the resistance of the brush hurries up the commutation towards the end of the interval (see curve u in Fig. 421), while if the E.M.F. is too great, the resistance prevents excessive over-commutation and forces the reversed current to come down to the normal value just before the brush leaves the bar (see curve o in Fig. 421). The effect of the brush is to give something in the nature of a back E.M.F., which opposes the flow of current out of the leaving bar (assuming the bar is positive). The back E.M.F. which the brush exerts is only

of small value (some two or three volts), so that if the adjustment of the commutating pole is so imperfect as to call for a greater correcting influence than can be exerted by the brush, sparking will occur. It is not well to rely on the brush to correct an error in the adjustment of the commutating voltage of more than 1 or 1·5 volts. For this reason one tries to keep below 10 volts the commutating voltage required to reverse the current in an armature coil, so that if there be a 10 per cent. error in the adjustment of the pole, the carbon brush can prevent sparking. If we have a commutating voltage of 20 volts, an error of 10 per cent. would require the brush to exert a back correcting pressure of 2 volts, and the commutation might not be satisfactory. Certain kinds of brushes are capable of giving up to 3 volts correcting E.M.F. before showing much sign of distress.

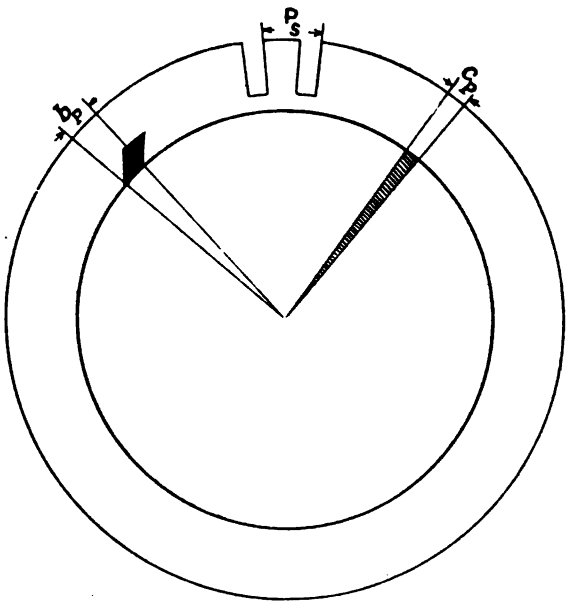


FIG. 424.—Showing p_s, the pitch of the slots; b_p, the breadth of the brush referred to the periphery; c_p, the breadth of a commutator bar referred to the periphery.

The main difficulty in providing this commutating pole and exciting it by the right amount of ampere-turns arises from the fact that the armature is itself a powerful electro-magnet, one of whose poles is directly on the place where we wish to fix the commutating pole, and the direction of the magnetization of the armature tends to produce a flux of the opposite kind to that required for commutation. If, therefore, we put an iron pole-piece in the place where the commutating pole is desired, the armature magnetomotive force tends to produce a flux through that pole which is of the wrong polarity; and before we can begin to get a flux of the right polarity we must put a number of ampere-turns on the commutating pole, which is greater than the number of ampere-turns operating in the armature. Having balanced the armature magnetomotive force, we must put on enough additional ampere-turns to produce a flux of the right value to bring about good commutation. Thus we have on the armature and commutating pole two powerful magnetomotive

forces which oppose one another; and it is only the difference between them which is effective in producing the commutating field. Thus it comes about that there is often a very heavy magnetic leakage from the commutating pole to the adjacent main pole, and this magnetic leakage tends to bring about the saturation of the iron on the commutating pole at heavy loads, and destroy the correct proportionality of the commutating flux. For this reason the cross-section of the iron on the commutating pole, particularly near the root, should be fairly heavy. A calculation should be made of the magnetic leakage, and sufficient iron provided, so as to avoid saturation. This is of particular importance on commutating poles, because the flux is produced by the difference between two magnetomotive forces; and as the difference is not very great compared with either one of them, a very little reluctance added to the magnetic circuit destroys the proportionality of the flux. The number of effective ampere-turns to be put upon the commutating pole

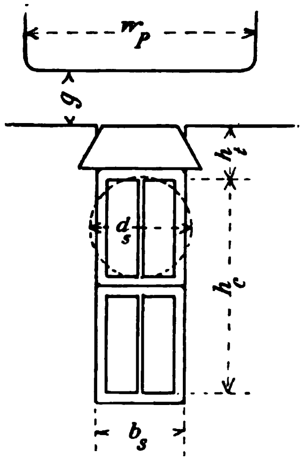


FIG. 425.

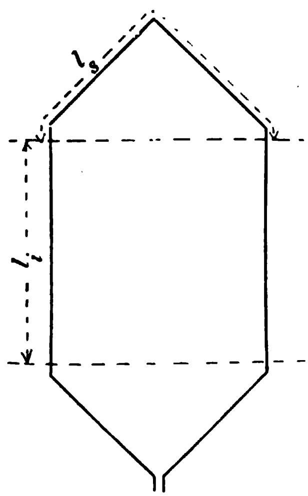


FIG. 426.

can be arrived at by the following considerations: Take all the conductors in any one slot, and see what change of total current occurs in those conductors as the slot moves past the brush. With a full-pitch winding the whole of the current in the slot will have been reversed during the interval in which the commutator bars to which the conductors are connected pass under the brush. If p_s is the pitch of slots in centimetres, and b_p the breadth of the brush increased in the ratio of $\frac{d_a}{d_c}$, and c_p the width of the commutator bar increased in the ratio $\frac{d_a}{d_c}$, where d_c is the diameter of the commutator and d_a is the diameter of the armature, then the interval of time taken for the current in the slot of an armature with a full-pitch winding to completely reverse is $\frac{p_s + b_p - c_p}{v_a}$, where v_a is the peripheral speed of the armature (see Fig. 424). In considering the leakage flux per centimetre length of iron around the conductors lying in one slot of a full-pitch winding per ampere passing, we shall make use of various dimensions indicated in Figs. 425 and 426.

We shall use the following symbols for the magnetic flux per centimetre of axial length of iron per ampere in the slot:

L_n = the effective flux crossing the body of the slot.
L_k = the flux bridging from the tops of the teeth along the air-gap.
L_c = the flux bridging across to the commutating pole and back again.
L_s = the flux encircling the end connections of the armature coil.

In the case of a wire-wound armature coil we may take

$$L_n = 1{\cdot}257\left(\frac{h_c}{3b_s} + \frac{h_t}{b_s}\right).$$

In the case of strap coils, the magnetic flux through the strap cannot change quickly on account of the eddy current generated in the copper. In this case, as a rough approximation, we may take

$$L_n = 1{\cdot}257\left(\frac{h_c}{6b_s} + \frac{h_t}{b_s}\right).$$

We have

$$L_k = 0{\cdot}92\log_{10}\left(\frac{\pi p_s}{2b_s}\right),$$

$$L_c = 1{\cdot}257\frac{w_p}{4g}$$

and

$$L_s = 0{\cdot}46\frac{l_s}{l_i}\left(\log_{10}\frac{l_s}{d_s} - 0{\cdot}2\right).$$

Where there is a commutating pole, it is usual to neglect the term L_k, because L_c takes its place, except in those cases where the length of air-gap under the commutating pole is very great.

Let us denote the sum of the leakage fluxes per centimetre length of iron by L_t. Then

$$L_t = L_n + L_c + L_s$$

in the ordinary commutating-pole machine.

Now, if the axial length of the commutating pole is the same as the axial length of the iron, the flux-density B_c in the gap necessary to bring about good commutation * will be

$$B_c = 2 \times L_t \times \frac{\text{amperes per slot}}{p_s + b_p - c_p},$$

* The proof of this formula is as follows. Assume that B_c is constant over a part of the periphery of the armature, having a width $p_s + b_p - c_p$. The conductors moving under the commutating pole, by reason of their motion, cut the flux from the pole $B_c \times (p_s + b_p - c_p) \times l_i$, while the current in one slot is being reversed. During the same period the flux created by the current in the slot changes from positive to negative, so that the cutting due to this latter cause is $2 \times L_t \times l_i \times$ amperes per slot. In order that these two effects shall neutralize each other

$$B_c \times (p_s + b_p - c_p) = 2L_t \times \text{amperes per slot}.$$

The following references to papers on the theory of the commutating pole will be of service:

Arnold and La Cour, "Commutation," *Trans. Internat. Elec. Cong.*, p. 801, 1904; see also Worrall, *Journ. I.E.E.*, vol. 45, p. 480; Page and Hiss, *ibid.*, vol. 39, p. 570; "Improved Arrangement of Commutating Poles for Dynamos," *Engineer*, 105, p. 181, 1908; "Motors with Commutation Poles," W. Siebert, *Elektrot. Zeitschr.*, 30, p. 465, 1909; "Interpole Designs," W. B. Hird, *I.E.E. Journ.*, 43, p. 509, 1909; "Auxiliary Poles for Direct-current Machines," J. N. Dodd, *Amer. I.E.E.*, Proc. 28, p. 467, 1909; "Reactive Effect of Auxiliary Poles in D.C. Machines," F. Punga, *Elek. u. Maschinenbau*, 29, p. 306, 1911; "Design of Auxiliary Poles," A. Brunt, *Elec. Rev. and West. Electr.*, 59, p. 507, 1911; "Hunting of Direct-current Interpole Motors," E. Rosenberg, *Electrician*, 67, p. 670, 1911; "Calculation and Experimental Determination of Mean Reactance Voltage," J. Liska, *Elek. u. Maschinenbau*, 30, p. 825, 1912; "Leakage Coefficients of Commutating Poles," L. A. Doggett, *Electrician*, 69, p. 821, 1912; "Calculation of Interpoles," De Bast, *Assoc. Ing. El. Liège*, Bull. 13, p. 208, 1913; "Armature Reaction and Characteristic Curves of D.C. Dynamos," Guilbert, *Lumière Elec.*, 22, p. 69, 1913.

where p_s, b_p and c_p are measured in centimetres, and have the signification shown in Fig. 424. This is on the assumption that we wish to bring about commutation according to a straight-line law. If we wish the commutating curve to follow approximately a sine curve, then we will have to shape the commutating pole so as to give a fringing flux, and the maximum value of B_c will be obtained from the expression

$$B_c = 2{\cdot}8 \times L_t \times \frac{\text{amperes per slot}}{p_s + b_p - c_p}.$$

A somewhat similar effect can be obtained by making the throw of the armature coil one slot smaller than a full pitch, and making the width of the commutating pole just about one slot pitch. We then get a commutation curve like that shown in Fig. 423.

If the axial length of the commutating pole shoe, l_c, is less than the axial length of the armature iron, l_i, then two corrections are necessary: in the first place L_c must be reduced in the ratio l_c/l_i, and in the second place we must increase B_c by the ratio l_i/l_c.

If the armature has not a full-pitch winding, the coil undergoing commutation under a N. pole will not lie in the same slots as the coil undergoing commutation under a S. pole. The interval of time during which the leakage flux across a slot is reversed will be twice as long. The effect is approximately the same as if the flux leaking across the slot were reduced to one half, so for short throw coils we divide L_n and L_k by 2. L_c and L_s are, however, practically unaffected.

Magnetic oscillations. Besides the E.M.F. produced in the armature conductors by the reversal of the leakage flux above-mentioned, there are other E.M.F.'s which must be guarded against. If the pole is not bevelled and the slots per pole are few, the swinging of the flux (considered on page 313) and the movement of the conductors under the pole produce ripples in the continuous voltage generated between a pair of brushes. The current supplied by the generator will also have ripples in it, and these set up high frequency E.M.F.'s in the conductors under commutation, and may cause sparking troubles. For this reason it is not well to have too few slots per pole. The fringing flux from the corner of the pole may cause trouble in the conductors under commutation if it swings about and generates E.M.F.'s which are not proportional to the velocity of the conductors. This trouble is obviated by bevelling the pole and by arranging to have at least three or four slots between the main poles. Flux swinging may occur under the commutating poles themselves and produce alternating E.M.F.'s which are superimposed upon the legitimate E.M.F. of commutation. The greater the number of slots in the commutating zone and the greater the air-gap under the commutating pole, the less these effects will be. The most perfect way of getting rid of these effects is to mount the punchings of the armature so that the slots are not parallel to the axis of the generator, the position of a slot at one end of the armature being exactly one slot pitch further ahead than the position of the slot at the other end of the armature, as shown in Fig. 533. This skew mounting of the punchings may be either on the rotor or on the stator. Sometimes the edge of the pole, instead of being parallel to the axis of the machine, is given an inclination to the axis, so that one corner is one tooth pitch further ahead than the corner at the other

end of the armature. Upon the whole, the bevelling of the pole is the cheapest method, and has the additional advantage that it diminishes the noise caused by the rotation of the armature at the same time as it diminishes the magnetic effect which we have been describing.

Brush gear. The problem of how to collect an electric current from a quickly-moving conducting surface has been a very difficult one, and it has only been partially solved. In the early days of the dynamo, nothing seemed easier than to use a metal wire brush on a copper commutator. But the wear between metal and metal is too great in these days, when machines are expected to yield thousands of amperes day after day with little or no attention.

The use of a carbon brush on a copper commutator gives us very little wear when the materials are of the right quality. Under good conditions, a carbon brush takes a highly-polished surface, which makes no impression mechanically on the tough copper of the commutator.

If no current passed between the commutator and brushes, most commutators would run for years without showing any appreciable wear. It is not mechanical wear that gives trouble. The main difficulty arises in keeping the working face of the brush in close contact with the commutator. If there is any distance at all between the copper and the carbon, the current can only pass from one to the other by means of a short arc. It is this arc that causes nine-tenths of all the commutator troubles brought to the notice of the designers of continuous-current machines. If this arc is extremely short (less than one ten-thousandth of an inch) it may not do more than provide the requisite voltage drop between carbon and copper. When it assumes a length of one or two ten-thousandths, it begins to be troublesome, and may have the effect, when the current is passing from copper to carbon, of taking copper off the commutator and plating it on to the brushes. Thus, if some of the bars of a commutator are just a little lower than others, so that the carbon brushes do not quite touch them, there will be a tendency for copper to come off the low bars and make them lower still; and after a few weeks' running a " flat " develops on the place where previously the lowness of the bars could not have been detected—except, perhaps, by a slight difference in the colour.

The necessity of keeping the carbon brushes in close contact with the commutator, makes the design of the brush holder a matter of the greatest importance. It is not within the province of this book to treat at length upon the mechanical design of brush holders—that subject would require a book to itself. All that can be done here is to point out the main features that a good brush holder should possess.

A brush holder should be firmly supported. Not only should the arms which support the holders be stiff, but the part of the machine to which they are attached must be very rigid. The construction of the box of the holder, or the part which holds the carbon, should be sufficiently rigid to resist any distorting forces that come upon it during the running of the machine.

While the carbon must be free to follow any slight eccentricity in the commutator, it must be held so that it does not tilt or change the angle of the face presented to the commutator. For this reason the " box type " holder, in which the carbon can slide parallel to itself, has found more favour than the pivotted holder, in which the carbon tilts through a slight angle when the commutator runs out of truth.

Box holders have sometimes the drawback that they do not fit the brushes well enough. Either the brush is so tight that it cannot slide, or it is so loose that it shakes about. For this reason, it is best to supply the box with a side spring that keeps the brush pressed lightly but definitely against the side of the box against which it is intended to slide.

The holder should be provided with a spring for pressing the carbon against the commutator; and this spring should be capable of easy adjustment while the machine is running. A pressure of 1 lb. to $1\frac{1}{2}$ lbs. per square inch of contact area is generally sufficient. In cases where the commutator can be made to run very true, smaller pressures can be used successfully.

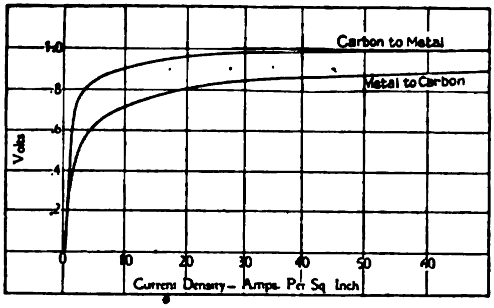


FIG. 427.—Giving the approximate voltage drop at brushes under good conditions with ordinary carbon brushes.

The brush holder should be so made that the brushes can be taken out and inspected readily without altering the adjustment of the spring tension. Nearly all brushes are now supplied with flexible leads to carry the current from the brush. These flexibles should be made very ample, because it is found in practice that a brush is often called upon to carry much more than its share of the load.

Resistance of the brushes. The voltage drop which occurs at the contact surface of the brushes when current is passing depends upon (*a*) the kind of brushes, (*b*) whether the brush is positive or negative, (*c*) the current density, (*d*) the mechanical pressure employed, and (*e*) the state of the commutator.

With brushes of ordinary hard carbon, the voltage from copper to carbon and from carbon to copper varies with the current density,* as shown in Fig. 427. These results were obtained on a commutator which had been a long time in service, so that it had acquired a fine polish and was completely free from low bars or high mica. The pressure employed was $1\frac{1}{2}$ lbs. per sq. inch. Where a higher mechanical pressure is employed, the voltage drop may be lower. Fig. 428 shows how the voltage drop changes with the mechanical pressure. In the experiments from the results of which these curves were plotted the contact conditions were not as good as in the experiments recorded in Fig. 427. The voltage drop remains practically constant for commutator speeds between 2000 and 5000 feet per minute.

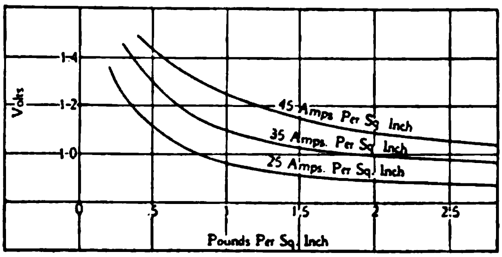


FIG. 428.—Giving the approximate voltage drop at brushes with different pressures.

Some soft graphite brushes are specially designed to give a low contact drop. For instance, the "L.F.C. 2" brush of the Le Carbone Company, when worked

* Arnold and La Cour, *Trans. Internat. Elec. Cong.*, 1904, p. 801.

at a current density of 55 amperes per sq. in. and at a mechanical pressure of $1\frac{3}{4}$ lbs. per sq. in., gives a total drop of only 1·2 volts on positive and negative brushes taken together. The Morgan Crucible Co. also make graphitic brushes giving a very low contact drop. Where the commutator runs very true and where the mechanical conditions are exceedingly good (as, for instance, of the radial commutator illustrated in Fig. 437), voltage drops as low as 0·4 have been obtained at pressures not exceeding $1\frac{1}{2}$ lbs. per sq. inch on graphitic brushes.

Where a certain amount of copper is added to the carbon, the voltage drop is reduced, but the addition of this copper always tends to increase the coefficient of friction between the brush and the commutator, and when enough copper is added to substantially lower the brush drop, it is found that the wear on the commutator is very much increased.

The **coefficient of friction** of a hard carbon brush working on a commutator in perfect condition will vary from 0·3 for a speed of 500 feet per minute to 0·25 for a speed of 5000 feet per minute. For graphitic brushes the coefficient of friction varies from 0·27 for a speed of 500 feet per minute to 0·15 for a speed of 5000 feet per minute. The coefficient of friction, however, is a very uncertain quantity, and varies very greatly for slight differences in the state of the commutator surface, and with the type of brush-holder employed. In calculating the losses due to friction, it is well to take the coefficient at 0·25 for graphitic brushes and 0·3 for hard carbon.

Characteristic curves. A great deal has been written on this subject in books dealing with the theory of continuous-current machines, so that it is unnecessary to discuss it here. Where a machine is compound wound the manner of determining the number of turns of series winding is in practice very simple. A magnetization curve (see pages 281 and 489) is plotted, and the number of ampere-turns required to give the increased voltage on full load ascertained, allowance being made for cross-magnetization and voltage drop in the armature and brushes. The shunt ampere-turns at full voltage are deducted from these, and the remainders give the required series turns. In practice one puts on a few ampere-turns in excess, because it is so easy to adjust the machine when it comes on test by means of a diverter.

THE SPECIFICATION OF CONTINUOUS-CURRENT GENERATORS.

Regulation. There are not so many qualities to take into account on a continuous-current generator as in the case of some of the machines dealt with in other chapters; and the specification can therefore be made very short and simple. One feature which the purchaser or his adviser must look to is the regulation-characteristic. This will depend on the nature of the service for which the generator is intended. For traction work it is usual to install compound-wound generators, and in the past 10 per cent. over-compounding has commonly been asked for. This is in general more than sufficient to compensate for the drop in feeders. The purchaser should make a rough estimate of how much over-compounding will be required to make the operation satisfactory in practice, and not call for more than he requires.

When calling for compound-wound generators, the specification should state whether the series winding is to be connected on the positive or negative side of the machine. Where generators are already installed with which the new machine must run in parallel, it is well to state the voltage which at present exists between the equalizer bar and the positive bus-bar (if the series winding is on the positive side) at full load on the power-house. This will enable the designer of the new machine to adapt the resistance of his series winding in the most economical way. It is also well to state what the actual rise of voltage is between no load and full load. The designer of the new machine, having found out the change in speed which will occur with the prime mover, can arrange his winding and the characteristics of his machine to meet the conditions.

For ordinary town lighting and power supply, it is usual to employ shunt machines, either hand regulated or controlled by an automatic regulator.

Where shunt machines intended for important work are specified, it is well to give the characteristics of the generators at present installed and the actual drop in voltage when full load is thrown on and after the prime mover has settled down to its full-load speed.

If the duty for which the generator is required is specified, it will sometimes assist the manufacturer to adapt his machine more exactly to meet the required conditions.

The first specification which we give as an example relates to a small generator. For this the specification should be as simple as possible, and should not contain any clause which will prevent a manufacturer quoting on his standard machine, otherwise the prices quoted will probably be higher than lowest competitive prices. The requirements in performance only should be stated. It is not wise to call for a certain efficiency : it is better to ask the Contractor what the full-load losses on his machine are, and then, on comparing tenders, an allowance can be made in prices on the basis of the losses.

SPECIFICATION No. 10.

75 K.W. CONTINUOUS-CURRENT BELT-DRIVEN GENERATOR.

Clauses 1, p. 269; 21, p. 333; or 170, p. 519.

Characteristics of Generator.

150. There shall be supplied a shunt-wound continuous-current generator having the characteristics set out below:

Normal output	75 K.W.
Normal voltage at terminals	525.
Voltage adjustment on rheostat	500 to 530.
Normal current	143 amperes.
Speed	750 R.P.M.
How driven	Belted.
Size of steel pulley to be supplied	12 in. dia., 10 ins. wide.
Temperature rise after 2 hours full load run	45° C. by thermometer. 55° C. by resistance.
Over load	25 per cent. for 30 minutes.
Temperature rise after 30 minutes over load	60° C. by thermometer. 70° C. by resistance.
Puncture test	1500 volts (alternating) applied for 1 minute between windings and frame.

Pulley.

151. The generator shall be provided with a pulley of the size above specified and be mounted on slide rail with belt-tightening screws.

Delivery.

152. The contract includes the delivery of the generator at the purchaser's works in   , but does not include erection or starting-up.

Statement of Losses.

153. The contractor shall state the amount of the following losses in the generator which he supplies:

1. Bearing friction and windage losses (at no load).
2. Iron losses (at no load).
3. Armature and field copper losses at full load, allowing for temperature rise.

154. The generator shall be run for two hours at full load at the contractor's works in the presence of the purchaser's engineer, without showing any sparking at full load, and without injurious sparking on over load. At the time of this run tests shall be made to see if the machine has the characteristics set out in Clause 150, and measurements shall be made of the losses above specified. If it is found to comply with all the conditions it shall be accepted without further tests. But if during the first six months after delivery any defects in the construction or performance become manifest, the same shall be immediately rectified by the contractor at his expense. Any time between the reporting of defects and the remedying of same shall not be counted in the six months' period of maintenance. Tests.

DESIGN OF A 75 K.W. CONTINUOUS-CURRENT GENERATOR.

525 volts; 144 amperes; 750 R.P.M.

Small belted motors and generators are now usually built with a fan at one end of the armature, of the kind illustrated in Fig. 429. By this means such very good ventilation is ensured that fairly large outputs can be obtained from small frames; in fact, for a machine not larger than 75 K.W., it is possible to take a D^2l constant of 4×10^5 cu. cms. In fixing diameter and length, we must remember that the machine will (if it is to be economically manufactured) constitute one of a line of generators whose output may vary from 1 K.W. to 100 K.W.; and in such a line it is usual to build several machines of different outputs on the same diameter, the length of iron being changeable so as to make an economical machine for each output. It thus comes about that any given machine in the standard line may either be of moderately large diameter and short length, or of smaller diameter and greater length, according to the accident which puts it upon one frame rather than the frame smaller. A considerable difference of opinion still exists between designers as to how far it is economical to lengthen the core of a given frame parallel to the shaft before going up to the next frame with a shorter core. It appears, however, that if we take into account the cost of material and labour on machines manufactured in large numbers, the most economical machine will be one which has a ratio of length of iron to pole pitch lying between the limits 0·5 and 0·8; and without going into the labour and material costs in great detail in any given factory, it would be difficult to state more exactly the best possible dimensions. In fact, even if the ratio of length of iron to pole pitch lies considerably outside the above-mentioned limits, it does not follow that the cost per kilowatt will be very much increased. The best ratio of length of iron to pole pitch is largely dependent upon the question whether the designer chooses to build a "copper" machine—that is to say, one in which the I_aZ_a is great—or an "iron" machine—one in which the $A_g\mathrm{B}$ is great (see page 8).

This ratio of length of armature iron to pole pitch is also controlled by the shape of the field pole. If we decide to use round poles, we cannot well, on a four-pole machine, make the ratio much greater than 0·8. It will, however, be possible with round poles to have three standard sizes of frame, using different diameters of pole body: three convenient ratios in this case are 0·5, 0·65 and 0·8.

For the 75 K.W. machine under consideration, we will adopt the ratio 0·65, making the diameter of the armature 43·5 cms. and the length 22 cms.

A calculation sheet is given on page 489. We begin by filling in the main data of the machine.

The best number of poles to take when designing a C.C. generator is controlled partly by the considerations given on page 10, and partly by consideration of the amperes to be collected at each brush arm. Where a generator is of small output and the total current is not great, say under 800 amperes, there is no advantage to be gained in making more than four poles. An increase in the number of poles increases the cost of labour. Moreover, with small machines, it would be difficult to get in enough commutator bars per pole if there were six poles. Four-pole machines are cheaper than two-pole machines, for the reasons given on page 12, except for very small sizes, where the labour is the main consideration. Where the current output is very great, the number of poles is increased so that the current per brush arm may not be excessive. Five hundred amperes per brush arm can be dealt with very satisfactorily with properly designed commutating poles.

Other considerations controlling the number of brush arms are given on page 567.

We will therefore choose four poles for this machine and proceed to fill up the calculation sheet.

To obtain 525 terminal volts at full load, we should allow for the generation of 540 volts at no load. The amperes per terminal will be 144, the cycles per second 25, the speed 750 R.P.M., or 12·5 revs. per sec. With a two-circuit winding, the amperes per conductor will be 72; and with 4 brush arms the amperes per brush arm will be 72. The specified temperature rise is 45° C., and the over-load capacity 20 per cent. for one hour.

Type of winding. To generate 525 volts on a machine as small as 75 K.W., we should of course have to employ a two-circuit winding. The considerations which determine the choice of the kind of winding are given on p. 511. The method of settling approximately the number of conductors might be as follows:

Magnetic loading. The circumference of the armature is 137 cms., and the area of the working face, A_g, 3020 sq. cms. (see calculation sheet, page 489). If we work with a maximum flux-density of 8500, our A_gB will be $0{\cdot}256 \times 10^8$; and this will require, at a speed of 12·5 revs. per sec., about 250 conductors in series to give 540 volts (15 volts margin being allowed for drop in brushes and windings). The exact number of conductors to choose would depend upon the number of slots that we have in our standard punching. The number of slots in a standard punching will by preference be an odd number, so as to enable a series winding to be constructed without any idle coils; and by preference, for a four-pole machine, it will be a multiple of 4, plus 1. **The number of slots per pole** ought not to be less than 9 on a machine of this size, and about 10 slots per pole would be good practice. We will take 41 slots in all; 41 multiplied by 12 gives us 492, which divided by

Date 6 Jan. 1914 Type C.C. GEN. ~~SYN MOTOR ROTARY~~ 4 Poles Elec Spec. 10
K.W. 75; P.F. ; Phase ; Volts 525; Amps per ter 144; Cycles 25; R.P.M. 750; Rotor Amps ;
H.P. Amps p. cond. 72 Amps p. br. arm 72 Temp. rise 45° Regulation 10% drop Overload 20% ½ hour

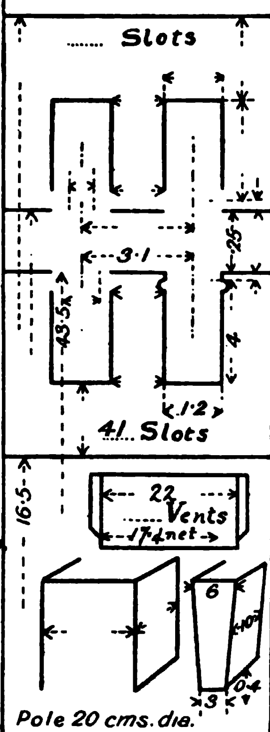
Customer ; Order No. ; Quot. No. ; Perf Spec. ; Fly-wheel effect

Frame 43/22 Air Circum. 137; Gap Area 3020; poss. $A_g B$; $A_g B$ ·256×10^8; poss. $I_a Z_a$; $I_a Z_a$ 35400; $I_a Z_a$ / Circum. 258; $D^2 L \times RPM$ / K.V.A. 4·1×10^5;

K_e ·685; ·540 Volts = ·685 × 12·5 × 246 × ·256; Arm. A.T. p. pole 4420; Max. Fld. A.T.

Armature. Rev. ~~Stat.~~

Core.	Dia. Outs.	43·5	
	Dia. Ins.	16·5	
	Gross Length	22·0	
	Air Vents 3	0·75	
	Opening Min. Mean		
	~~Air~~ Velocity		
	Net Length 19·5 ×·89	17·4	
	Depth b. Slots	9·5	
	Section 165 Vol.	13500	
	Flux Density	13200	
	Loss ·034 p.cu.cm Total	460	
	Buried Cu. 680 Total	840	1520
	Gap Area 3020; Wts	860	
	Vent Area 5000; Wts	420	
	Outs. Area 2300; Wts	350	1630
Teeth.	No of Segs Mn. Circ.	117·5	
	No of Slots 41 ×1·2 =	49·2	
	K_s 2·1	68·3	
	Section Teeth	1190	
	Volume Teeth	4750	
	Flux Density App	21,500	21,000
	Loss ·08 p.cu cm. Total	380	
	Weight of Iron	142 kilogs.	
Conductors.	Star or Mesh Throw	1 and 11	
	Cond. p Slot	12	
	Total Conds 246 in series		492
	Size of Cond 2(·25×·35)	2×·085	·17 sq cm
	Amp. p. sq.	425	
	Length in Slots 22		
	Length outside 54 Sum	·76	
	Total Length	374 m	
	Wt. of 1,000 151 Total	56·6 Kilogrs.	
	Res. p. 1,000 1 Total	·374/4	·094
	Watts p. m	75	
	Surface p. m	960	
	Watts p. Sq. cm.	·078	
	·15×·078 / ·0012	10° C	



Field Stat ~~or Rotor~~.

~~Dia.~~ Bore	44
½ Total Air Gap	·25
Gap Co-eff. K_g	1·25
Pole Pitch 34·4 Pole Arc	24·5
K_f	·685
Flux per Pole	4·35×10^6
Leakage n.l. f.l.	5·14×10^6
Area 314 Flux density	16400
Unbalanced Pull	
No. of Seg. Mn. Circ	
No. of Slots × =	
Vents	
K_s Section	
Weight of Iron	

	Shunt.	Series.	Comm.
A.T. p Pole n. Load	3841		
A.T. p. Pole f. Load	5200		5900
Surface	10,000		5200
Surface p. Watt	14		15
I^2 R.	720		345
I. R.	463		2·4
Amps.	1·55		144
No. of Turns	3400		41
Mean l. Turn	78		46
Total Length	10300 m		75 m
Resistance (cold) 250; (hot) 300.			·0195
Res. per 1,000	24·4		·22
Size of Cond	·007 sq cms.		·225×3·5
Conds. per Slot			
Total			
Length			
Wt. per 1,000	6·2		679
Total Wt.	64 kilogs.		54
Watts per Sq.	·072		
Star or Mesh			
Paths in parallel			

Magnetization Curve.

	Section.	Length	525 Volts B.	A.T.p.c.	A.T.	540 Volts B.	A.T.p.c.	A.T.	560 Volts B.	A.T.p.c.	A.T.
Core	165	10	12800	8	80	13200	10	100	13700	12	120
Stator Teeth											
Rotor Teeth	1190	4	20600	220	880	21,000	350	1400	22200	500	2000
Gap	3920	·25	8250		2090	8500		2150	8800		2230
Pole Body	314	13	15900	35	455	16400	40	520	17,000	45	585
Yoke	192	42	13,000	8	336	13400	9·5	400	13,900	11	462
					3841			4570			5397

Commutator.

Dia. 33 Speed	13 m.p. sec.
Bars	123
Volts p. Bar	17
Brs. p. Arm	2
Size of Brs.	2×4·5
Amps p sq.	cm 4
Brush Loss	300 Watts
Watts p. Sq.	·3

EFFICIENCY

	1¼ load.	Full.	¾	½	¼
Friction and W	1·0	1·0	1·0	1·0	1·0
Iron Loss	·92	·9	·88	·86	·84
Field Loss	·85	·82	·8	·78	·76
Arm &c. I^2R	4·1	2·75	1·5	·65	·15
Brush Loss	·38	·3	·23	·15	·07
	7·25	5·77	4·41	3·44	2·82
Output	94	75	56	35·5	19
Input	101·25	80·8	60·4	38·9	21·8
Efficiency %	92·7	92·8	92·7	91·1	87

Mag. Cur. Loss Cur.
Perm. ~~Stat.~~ Slot 1·57
„ ~~Rot. Slot~~ × End 1·32
„ Zig-zag 1·47
2 × × = 4·36
1·77 × × =
End × × =
Amps. Tot
τ = ; X_a =
S_1/S_2 , r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max Torque
Max. H.P.
Slip
Power Factor

2 gives us 246 conductors in series, a number sufficiently near to the preliminary number 250. It is necessary to know the pole arc before we can arrive at the constant K_e (see page 13). It will be seen from the drawing of the machine (Fig. 429) that we cannot well make the pole arc wider than 24·5 cms. This pole arc with the slight bevel shown gives us $K_f = K_e = 0{\cdot}685$ (see page 23). We can therefore write down the formula for the voltage :

$$540 \text{ volts} \times 10^8 = 0{\cdot}685 \times 12{\cdot}5 \times 246 \times A_g\text{B}.$$

This gives us $$A_g\text{B} = 0{\cdot}256 \times 10^8.$$

If we allow three ventilating ducts each 0·75 cm. wide, we get a net length of iron, after allowing for paper insulation, of 17·4 cms.

The size of the slots will generally be fixed by our standard dies ; and we should as a rule have to contrive, by using several conductors in parallel, to obtain the cross-section required in the space at our disposal. For this purpose **conductors** of rectangular cross-section are very much more convenient than round conductors ; and we have seen on page 151 that cotton-covered rectangular conductors can be used and safely shaped into armature coils if coils of the right type are employed. If, for instance, our standard slot has a depth of 4 cms. and a width of 1·2 cms., we can employ two conductors in parallel, each 0·25 × 0·35 cms., arranged one above the other as shown in Fig. 159, to constitute a conductor having a cross-section of 0·17 sq. cm. There will thus be six double conductors per complete coil. A rectangular conductor 0·25 × 0·35 when double-cotton covered will measure 0·28 × 0·38. Three of these side by side will take up 0·84 cm., leaving 0·36 for insulation and internal roughness of slot. This room will permit of three turns of paper and mica, together with one layer of linen tape to hold it in position. The room in depth of the slot will be found to be amply sufficient, and where too ample can be made up by means of strips of press-spahn inserted either in the bottom of the slot or between the limbs of the coils.

As the current per conductor is 72 amperes, the current density will be $72 \div 0{\cdot}17 = 425$ amperes per sq. cm. This we know from experience is not too high a current density in an armature of this type ; but strictly we ought to work out the cooling conditions for the slot as indicated on page 224.

It next remains to work out the **flux-density in the teeth.** The depth of tooth being 4, we take the diameter of the mean circle as 37·5, giving us a mean circumference of 117·5 cms. As there are 41 slots each 1·2 cms. wide, we subtract 49·2 and obtain 68·3 cms. as the total width of all the teeth. Multiplying this by the net length, 17·4, we obtain 1190 sq. cms. as the total section of all the teeth. Dividing this into $0{\cdot}256 \times 10^8$, we obtain 21,500 as the apparent flux-density in the tooth at one-third of its length from the root. As the ratio $K_s = 2{\cdot}1$ (see page 71), the actual flux-density in the teeth is 21,000. From Fig. 29 (page 51) we find the loss at 25 cycles (B = 21,000) is 0·08 watt per cu. cm. As the volume of the teeth is 4750 cu. cms., the loss in the teeth is 380 watts. A flux-density of 21,000 is not excessive for 25 cycles, and therefore the gross length of iron, 22 cms., is sufficient. If the density in the teeth had come out too high, we should have had either to lengthen the machine or take a different size of slot, and possibly a different number of slots with a different number of conductors.

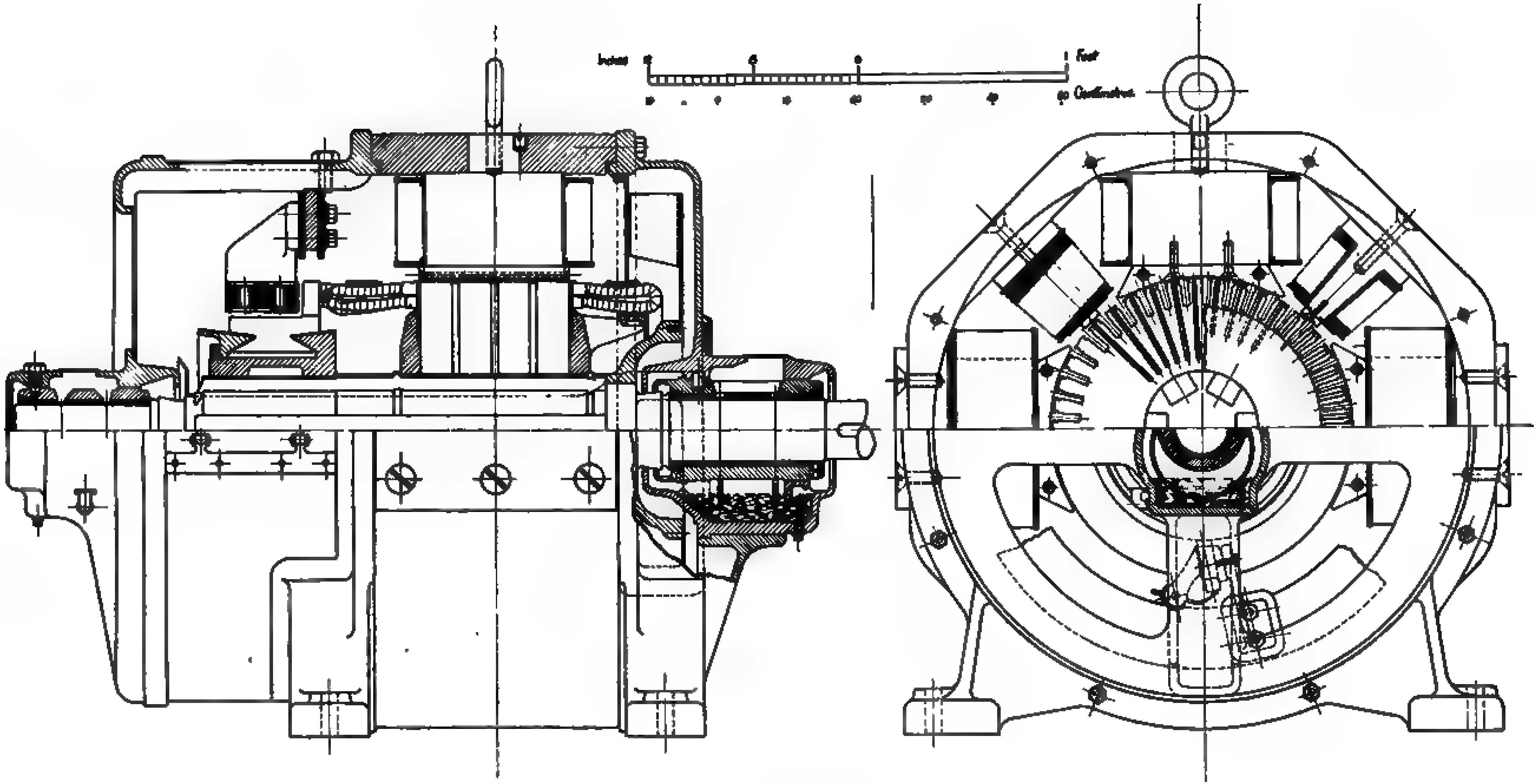


FIG. 420.—75 K.W. continuous-current generator, 525 volts, 750 R.P.M.

In order to calculate the loss in the iron behind the slots we must find the working flux per pole. This is

$$\frac{0{\cdot}256 \times 10^8 \times 0{\cdot}685}{4} = 4{\cdot}35 \times 10^6.$$

The cross-section of the core is 165 sq. cms., giving a flux-density of 13,200. This (from Fig. 29) gives a loss of 0·034 watt per cu. cm. As the total volume is 13,500, we have the iron loss behind the slot equal to 460 watts. This added to 380 gives 840 watts total iron loss. The loss on the part of the copper conductors of the armature which are buried in the slots (as calculated below) amounts to 680 watts, so that the total watts to be dissipated by the iron surfaces of the armature are 1520.

The number of **watts** which can be **dissipated from the surface** of an armature ventilated in any particular manner can only be ascertained by trial, and the ratings of all machines of this kind are really based on experiment. However, to illustrate the methods of calculating the temperature rise given in Chapter X., we will apply them to this machine. It will be found in practice that they give a very fair indication of what the temperature rise will be.

The most important cooling surface is the cylindrical surface of the armature. The velocity is 17 metres per second. Allowing 35° C. for the difference between the iron and air, we have

$$35 = \frac{333 \times \text{wts. sq. cm.}}{1 + 1{\cdot}7},$$

$$\text{Watts per sq. cm.} = 0{\cdot}285.$$

As the cylindrical surface is 3020 sq. cms., we can get rid of $0{\cdot}285 \times 3020 = 860$ watts.

The velocity of air in the ducts of these little machines is always an uncertain quantity, because it is so much affected by the obstructions in the path of the air. However, in a small armature with wide teeth and vents, not less than 0·75 cm. wide, it may be taken at $\frac{1}{5}$th the peripheral speed. In this case, say 3 metres per sec.

From page 242, we have $h_v = 0{\cdot}0014 \times 3 = 0{\cdot}0042$; allowing 20° difference of temperature between iron and air in the vents, we have

$$20 \times 0{\cdot}0042 \times 5000 = 420 \text{ watts dissipated by the sides of the vents.}$$

The outside of the end plates and the inside cylindrical surface present a cooling surface of 2300 sq. cms. Allowing 0·15 watt per sq. cm. (see p. 254), we get rid of an additional 350 watts, making the total 1630 watts for 45° C. rise. As the total watts to be dissipated by these surfaces are 1520, we are well within the guarantee.

We now come to the compartment of the calculation sheet marked "**conductors.**" The throw of the coils will be 1 and 11, the pitch being 10. There are 12 conductors per slot, making 492 in all, that is, 246 in series. The size we have already dealt with. The length in the slots is 22 cms. and the length outside can be found from the drawing, or, where no drawing is at hand, from the formula $(1{\cdot}4 \times \text{pole pitch}) + 5$. This gives us 54 cms., so the length of one conductor and its end connector is 0·76 metre. Multiplying by the number of conductors we get a total length of 374 metres. Multiplying 890 by 0·17 (see page 143) we get the weight of a 1000 metres $= 150$ kilograms, so that 376 metres weigh 56·6 kilograms. The resistance of 1000 metres

is found by dividing 0·17 by the section. This gives us just 1 ohm per 1000 metres, so that the resistance of all the conductors in series is 0·374 ohm. As there are two paths in parallel, we divide by 4 and get 0·094 ohm at 15° C. Now consider the cooling conditions. One metre length of coil containing 12 conductors, when hot, will cause

$$12 \times 0{\cdot}001 \times 1{\cdot}2 \times 72 \times 72 = 75 \text{ watts loss.}$$

The cooling surface of this metre length of coil will be 960 sq. cms., so the watts per sq. cm. = 0·078. As the thickness of the insulating wall is 0·15 cm., we have

$$\frac{0{\cdot}15 \times 0{\cdot}078}{0{\cdot}0012} = 10^\circ \text{ C.}$$

difference of temperature between the copper and iron of the armature. This is not too much. Where the watts per sq. cm. of cooling surface of coil are below 0·08 watt per sq. cm., the cooling conditions of the end-windings, over which the air is forced by the fan, are sufficiently good.

Ampere-turns per pole. We may conveniently work out the ampere-turns per pole for three different voltages, 525, 540 and 560 volts.

In a machine of this size the length of the air-gap is fixed from two considerations. In the first place, it must not be so small as to leave any danger of the armature coming in contact with the field poles after the bearings are somewhat worn. In the second place, it must be sufficiently great to prevent excessive distortion of the field by the armature magnetomotive force. In a machine having no compensating winding, it is desirable to have the ampere-turns on the field a little more than the armature ampere-turns per pole. In this case the armature ampere-turns per pole are equal to 4420. The field ampere-turns ought not to be less than 5200 at full load. A rough preliminary calculation shows us that the ampere-turns on the teeth and other parts of the magnetic circuit will amount to about 2500; and allowing 20 per cent. of the armature ampere-turns for the increase between the no-load and full-load excitation, say 900 ampere-turns, we should have on the air-gap about 2100 ampere-turns. The density in the gap at 540 volts is obtained by dividing $0{\cdot}256 \times 10^8$ by the gap area 3020. This gives us the flux-density in the gap of 8500. A rough preliminary calculation again gives us the length of gap at about 0·254 cm.; and taking this figure, we proceed to work out the magnetization curve. First note that K_f is in this case the same as K_e, viz. 0·685.

$$\text{Flux per pole} = \frac{0{\cdot}256 \times 10^8 \times 0{\cdot}685}{4} = 4{\cdot}35 \times 10^6.$$

The leakage, worked out by the method given on page 326, is equal to $0{\cdot}66 \times 10^6$ at no load and $0{\cdot}8 \times 10^6$ at full load.

In working out the magnetization curve, we will take first the volts as 540. The density in the core is obtained by dividing $4{\cdot}35 \times 10^6$ by 2×165 sq. cms., and is equal to 13,200. This requires 10 ampere-turns per cm., giving 100 ampere-turns on the core. The density in the rotor teeth has already been worked out at 21,000, and requires 350 ampere-turns per cm., or 1400 ampere-turns on the teeth. The gap coefficient K_g is in this case 1·25, so that with a flux-density of 8500 and a gap length of 0·254 cm., we have the ampere-turns on the gap equal to

$$8500 \times 0{\cdot}254 \times 1{\cdot}25 \times 0{\cdot}796 = 2150 \text{ ampere-turns.}$$

For the reasons given below, we will take a cylindrical pole body made of good iron; and as this may be worked at a density of about 16,000 C.G.S. lines per sq. cm., it may have a cross-section of about 314 sq. cm. The length of the pole body will depend upon the dimensions of our standard frame; in Fig. 427 we find it to be 13 cms. The ampere-turns per cm. at no load will be about 40, giving about 520 ampere-turns on the pole. The length of the yoke (see Fig. 427) is about 42 cms.; this is made of rolled steel ingot bent to shape, and has a cross-section of 192 sq. cms., giving a flux-density at no load of 13,400, requiring 9·5 ampere-turns per cm. This gives us 400 for the yoke. Thus the total ampere-turns per pole at no load are 4570 at 540 volts. In order to plot the magnetization curve, it is generally sufficient to take two other voltage points, say at 525 volts and 560 volts. The flux-density in the various parts are then best found from the slide-rule, being approximately proportional to the voltage. The form on page 489 gives the results. Thus we have 3841 ampere-turns per pole at 525 volts, and 5397 at 560 volts. In plotting the magnetization curve it will be found most convenient to take as ordinates the flux per pole instead of the voltage; so that in making calculations on the same frame (the number of conductors in the armature being such as to give the required voltage), we obtain the ampere-turns on the pole from the magnetic loading of the frame direct.

In a continuous-current generator, even when the brushes are placed upon the "neutral," it is found that it is necessary to considerably increase the ampere-turns at full load over the ampere-turns at no load in order to keep up the voltage. This increase is due in the first place to the resistance of the armature and brushes and of any series windings, and in the second place to the supersaturation of the teeth under the trailing horn, brought about by the cross-magnetization of the armature. Where a machine is fitted with commutating poles, the question whether there is any demagnetizing effect of the armature depends upon the exact position of the brushes on the commutator, and on the strength of the commutating pole. Where the strength of the commutating pole is such that commutation takes place behind the no-load neutral plane, the armature will have (on a generator) a magnetizing effect instead of a demagnetizing effect, and the extra ampere-turns put upon the field magnet in this way may be made to compensate for the drop in voltage which would otherwise be caused by the cross-magnetization effect and consequent supersaturation of the teeth.

As it is always possible, after a machine comes on test, so to adjust the strength of the commutating pole and the position of the brushes as to get a sufficiently small drop in voltage between no load and full load, an exact calculation as to the amount of extra ampere-turns required on the field pole to compensate for the supersaturation of the teeth on load is not usually necessary. A generator with its brushes rocked too far back will not run well in parallel with another generator, so that it is not advisable to depend too much upon this compensating effect. It is well to allow for an increase in the field-turns of some 10 per cent. on full load. With an armature such as we have under consideration, the ampere-turns in which are 0·9 of the field ampere-turns at no load, and in which the teeth absorb nearly one-third of the total ampere-turns on the pole, the increase in the ampere-turns at full load may be taken to be about 15 per cent. of the no-load ampere-turns.

Thus, to obtain 540 volts generated (that is, 525 volts at terminals), we will require 5200 ampere-turns per pole. It is well to design the shunt coil so that it will take continuously this full excitation, instead of relying upon the rocking back of the brushes to supply the extra ampere-turns needed on full load.

Having decided upon the maximum number of ampere-turns required on the shunt coil, the **number of turns of wire** will be settled from one of two considerations :

(1) We may wish to build the generator as cheaply as possible, using the smallest amount of wire that will give us a temperature rise not greater than the guaranteed temperature rise.

(2) We may be ruled by considerations of efficiency and settle the number of watts which are to be wasted in shunt excitation.

A large number of buyers will buy the cheapest machine that appears to be good enough for their purpose. Other buyers, on the other hand, recognize that very often a more expensive machine of higher efficiency will save more in the year than the interest on the extra cost. With power at one halfpenny per unit, a kilowatt for twelve hours a day for 300 days in the year will cost £7. 10*s*. per annum. Capitalizing this at 10 per cent., we get £75. It would in many cases be worth while for a buyer to pay £75 more for a machine which will save him 1 kilowatt in the shunt excitation.

In the machine worked out on page 489, the loss in the shunt coils and rheostat is 820 watts. The weight of copper is 64 kilograms. This is almost the minimum weight we could use if we are to meet the temperature guarantees. It would be good policy to increase this weight and make a saving in shunt losses if the buyer would recognize the fact, and pay a greater price. There is room for another 1500 turns, which would reduce the losses by 250 watts. This, on the above basis of calculation, could be capitalized at £19, and yet the cost of the extra 1500 turns would not be more than £8. Yet so keen are many buyers to buy the cheaper machine, heedless of small differences in efficiency, that the practice of using the least possible quantity of copper pays from the manufacturer's point of view.

The same want of regard on the part of the buyer for small differences in efficiency leads many manufacturers to use ordinary dynamo steel of good quality at (say) £11 per ton rather than alloyed steel at £25 per ton. In the present case, with ordinary iron the iron losses work out at 840 watts, whereas with alloyed steel they could be reduced certainly to 600 watts. The saving of 240 watts is worth about £18, and the cost of the alloyed iron would not be more than £5. Some buyers are beginning to recognize these facts, and the future may see a very great increase in the efficiency of small generators and motors.

Rectangular coils *versus* circular coils. A good deal of discussion has taken place between designers on the merits and demerits of coils wound on a cylindrical former and coils wound on a rectangular former. The advantages of the cylindrical coil are as follows :

(1) The length of turn for a given area enclosed is only 0·89 of the length of a turn for a square coil, and a smaller fraction still of a turn of a coil whose length is greater than its breadth.

(2) It can be wound by means of a machine, so that the labour in winding is considerably reduced.

(3) No insulation is required other than the cotton covering between layers; whereas with rectangular coils it is usual, when winding the wire "layer for layer," to insert insulation at the corners, in order to enable the wires to be drifted over as each layer is put on.

(4) The cylindrical coil, when wound "layer for layer," can be made much tighter and more compact than is possible with a rectangular coil, and the heat conductivity is therefore much increased.

(5) The bobbins are exceedingly cheap and easy to manufacture.

(6) The number of moulds to be kept in stock is reduced.

The disadvantages of the cylindrical coil are:

(1) It takes up more room measured along the periphery of the armature than a rectangular coil enclosing the same area. It, therefore, does not allow so much room between poles. This does not matter so much on four-pole machines on account of the great angle between the centre lines of the poles. If the axial length of the machine is not more than 0·8 of the pole pitch, the round pole limb leaves plenty of room for the insertion of a commutating pole and winding.

(2) It is not so easy to change the axial length of a frame when it is fitted with round poles. It is, however, possible to design a standard line of machines with three or more economical axial lengths.

We have adopted the round pole and cylindrical coil for four-pole machines, because we believe that there is nothing to be gained by making the axial length of

FIG. 430.—Showing arrangement of round steel pole body and rectangular pole shoe.

the armature greater than 0·8 of the pole pitch, and up to this length the round pole can be used. In laying out a standard line of frames, there might be three different axial lengths for armatures 43·4 cms. in diameter: 27 cms., 22 cms. and 17·5 cms. For these, three different diameters of round poles could be used, 22 cms., 20 cms. and 18 cms. The same punching for the pole shoe can be used in all cases, built up to different lengths. The punched pole shoe (see Figs. 429 and 430) is secured to the pole as follows: After building up the shoe and riveting together by means of

two axial rivets, four suitable points are chosen on the face which is to lie adjacent to the pole limb, and the iron of the punchings at these four points is melted together by means of an oxy-acetylene flame. These four points are then drilled and countersunk to receive screws which are screwed into the pole limb. On the pole limb 22 cms. in diameter it is necessary to use a built-up winding of partly conical form; but the other two take plain cylindrical coils which are exceedingly cheap to manufacture.

Yoke. The yoke may either be of cast steel and be cylindrical in form, as shown in Fig. 431, or it may be made of rolled ingot bent into a cylindrical or octagonal

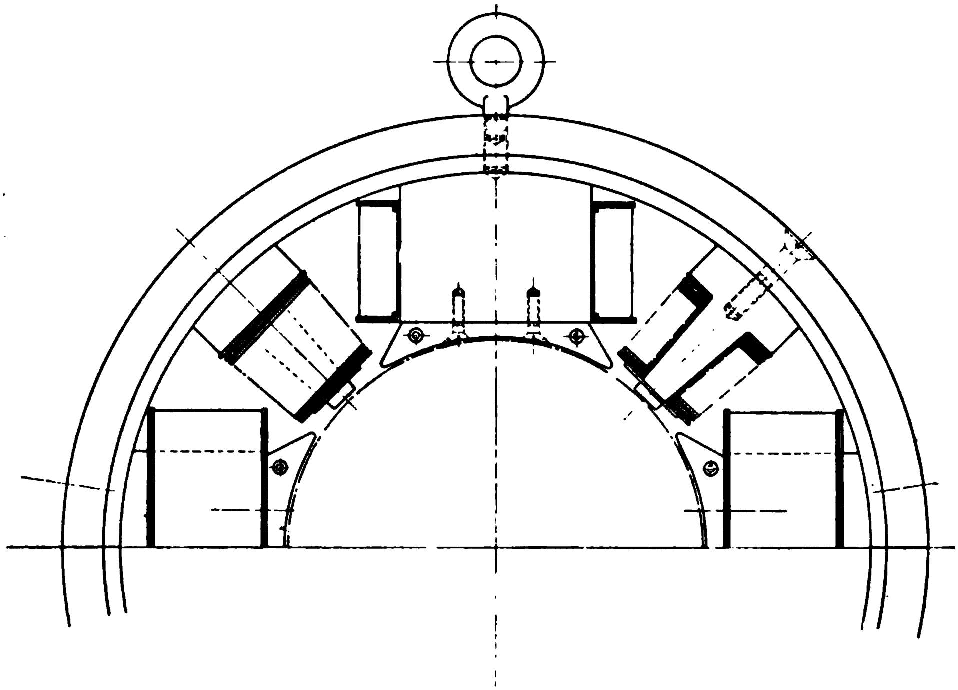


FIG. 431.—Showing arrangement of circular yoke of cast steel for 75 K.W. C.C. generator.

shape.. Where a large number of yokes are made in a forge provided with suitable machinery, the labour of bending small yokes into shape is not excessive. A steel casting for the 75 K.W. generator under consideration would weigh 8½ cwt., and at 13s. per cwt. will cost £5 12s. before any machining is done on it. The metal of the yoke shown in Fig. 429 rolled roughly to size will weigh 7½ cwt., and at 9s. per cwt. will cost £3 7s. There is more than enough difference in the first cost of material to pay for the forging of the frame. In the octagonal frame (Fig. 429) the machining of the surfaces to receive the poles can be carried out with a pin-cutter mounted on the tool that drills the sockets for the poles. The only other machining is on the faces where the two halves of the yoke meet, and the turning of the ends to receive the cast-iron end brackets. It will be seen that the feet of the generator are cast with the end brackets.

Shunt winding. As pointed out above, the shunt coil has been worked out for the minimum weight of copper. Beginning with the ampere-turns required at full load, we make a preliminary estimate of the total cooling surface of the coils. This is 10,000 sq. cms. Allowing 14 sq. cms. per watt, we are able to get rid of 720 watts. The approximate voltage expended on the shunt coils is found by deducting about 50 volts from 525 to allow for some margin on the rheostat. Dividing this voltage into the 720 watts, we find the approximate exciting current and from it the number of turns. The mean length of turn can then be found approximately, and the total length of wire, and hence the resistance cold and hot. Having chosen a wire which gives us approximately the right resistance, we can go over the figures again and get the values as shown in the calculation sheet.

Commutating pole. In order to calculate the flux-density B_c under the commutating pole required to bring about commutation, we proceed as indicated on page 480.

$$L_n = 1\cdot25\left(\frac{3}{3\times1\cdot2}+\frac{0\cdot5}{1\cdot2}\right)=1\cdot57,$$

$$L_c = 1\cdot257\times\frac{3}{4\times0\cdot41}\times\frac{14}{22}=1\cdot47,$$

$$L_s = 0\cdot46\times\frac{48}{21\cdot6}\left(\log_{10}\frac{48}{1\cdot6}-0\cdot2\right)=1\cdot32,$$

$$L_n+L_c+L_s=4\cdot36,$$

$$2\cdot8\times4\cdot36\times\frac{12\times72}{3\cdot1+2\cdot65-1\cdot1}=2265=B_c.$$

If the axial length of the pole tip is 14 effective cms., and the length of the gap under the commutating pole is 0·41 cm., we require

$$2265\times\frac{22}{14}\times1\cdot25\times0\cdot41\times0\cdot796=1450 \text{ ampere-turns per pole.}$$

Add to this the armature ampere-turns of 4420, and we get about 5900 ampere-turns total. We will therefore require 41 turns, carrying 144 amperes.

The width of the pole is 3 cms. at the tip, and as the air-gap is 0·41, we will have a fringing field, which, in conjunction with the short-throw coil, will give a diminishing commutating E.M.F. towards the end of the period of commutation (see Fig. 423). For this reason we have taken the coefficient 2·8 instead of 2 in the formula given above. It will be seen from the drawing (Fig. 429) that we have made the commutating pole 6 cms. wide at the root so as to avoid saturation on considerable over loads. By keeping the base of the pole wide we are able to shorten the axial length, and thus to save a great amount of copper in the coil. It is in fact quite good practice to make the commutating pole of round section in cases where sufficient room can be found for the rather wider limb required in this case. If a 60 K.W. generator be built upon the same frame, but with 17·5 cms. length of iron instead of 22 cms., it will be found that the diameter of the main pole will be reduced, and this gives room for a round commutating pole of ample section.

The commutator. In designing a commutator for a small machine of this kind simplicity and economy are important. There is no great danger from expansion troubles such as occur on large commutators, so that it is sufficient to support the bars between V-rings, one of which is turned on a cast-iron bush which forms the main

support of the commutator, the other being a drop-forging pressed in by means of a screwed washer. The bars will, of course, be of drawn copper and the insulation of mica.

In the design under consideration, we have 123 bars, or 31 bars per pole. This is as great a number as one can economically provide on a small machine. The number is found to be amply sufficient where the current to be collected is only 144 amperes.

Width of brushes. In settling the width of brush to be used on a commutating pole machine, one must have regard to the length of arc over which the short-circuited coil travels before the short circuit is removed. As long as this arc lies well away from the horns of the main pole, the brush is not too wide. One may allow it to extend within such a distance of the on-coming pole as to have it moving in a field from that pole almost equal to the field of the commutating pole. We may then have quite good commutation at full load, but as the field of the main pole gets weaker on load and stronger on no load, it is advisable to shorten the arc so that it lies almost entirely under the influence of the commutating pole. If in the last moments of the commutating period the field strength is reduced (see Fig. 423), the adjustment of the commutating winding will be found somewhat easier. Within these limits the wider the brush used on a commutating pole machine the better, as the time of commutation is increased and the E.M.F. required is smaller. In the machine under consideration we have made the brush 2 cms. wide. This makes $p_s + b_p - c_p = 4{\cdot}65$ cms., that is to say, just 1·65 cms. wider than the tip of the commutating pole.

As we have put two brushes per arm with an area of 18 sq. cms., the amperes per sq. cm. are only 4. This is lower than it need be. A density of 6 would do, but it is not always possible to fit in standard brushes so as to give the most economical arrangement.

The **cooling surface of the commutator** works out at 1000 sq. cms., so with 300 watts lost we have 0·3 watt per sq. cm. There is no danger of overheating if we are not troubled with high mica or some other cause of bad contact between commutator and brushes. It is good practice to mill out the mica to a depth of 1 mm. Brushes of ordinary hard carbon are recommended on this machine.

Efficiency. The way of working out the efficiency is sufficiently clear from the calculation form. The windage is considerably increased by the addition of the fan shown in Fig. 429. A machine of this kind will have a friction and windage loss of about 600 watts without the fan, and about 1000 watts with the fan.

It is very desirable to see that the fan is not very much greater than is necessary for the purpose of keeping down the temperature. If the machine runs much below the guaranteed temperature rise, it should be rated for a higher output or the fan should be reduced so as to lower the windage losses.

The figures for the iron loss have been increased a little on load to allow for the increased losses on the teeth. The field losses taken should include the losses in the rheostat. The I^2R losses include those in the armature and in the commutating pole winding. The brush losses are taken as if the voltage drop in positive and negative brushes amounted to 2·1 volts. This is justified by the low current-density. The total losses at full load are 5·77 K.W., giving an efficiency of 92·8 per cent.

SPECIFICATION No. 11.

1000 K.W. CONTINUOUS-CURRENT GENERATOR TO FORM PART OF A MOTOR-GENERATOR SET.

155. This specification provides for the supply, erection, testing and setting to work of a continuous-current generator having the following characteristics:

Characteristics of Generator.

Normal output	1000 K.W.
Voltage adjustable between	460 and 500.
Full load current	2000 amperes.
Speed	246 R.P.M.
How driven	Direct connected to induction motor.
Temperature rise after 6 hours full-load run	45° C. by thermometer. 50° C. by resistance.
Over load	2300 amperes for 30 minutes.
Temperature rise after 30 minutes over load	55° C. by thermometer.
Puncture test	1500 volts (alternating) applied for 1 minute between windings and frame.

Excitation.

156. The generator is to be shunt wound.

Duty.

157. The generator is intended to supply continuous current for general lighting and power work for the Town of It is intended to run in parallel with other continuous-current shunt-wound machines, some of which are motor-driven and some steam-driven. The particulars of these machines are given in Schedule I.

Extent of Work.

158. The contract will include the delivery of the generator, together with bedplate, half-coupling, bearing, and pedestal, at the sub-station at ; and the erecting, aligning and coupling of the same to the 1500-H.P. motor described in Specification No. . The switchgear and cable work are provided for under another specification.

Foundations.

(See Clauses 6, p. 271; 36, p. 360; 74, p. 382; 272, p. 591.)

159. The frame shall be split horizontally and arranged so that the armature may easily be inspected and lifted out without dismantling the brush-gear. **Horizontally split.**

160. The generator shall be of the ordinary multipolar type with drum-wound armature. The armature coils shall be placed in open slots and held so that they can be readily renewed. **Type of Machine.**

161. The commutator shall be of ample proportions, constructed according to the best practice. It shall be thoroughly seasoned before delivery, and after having been ground true once on site shall not show any signs of high bars, high mica or appreciable eccentricity. The mica may be cut out for $\frac{1}{32}$ inch below the commutator surface if the Contractor will guarantee that no dirt will lodge in the grooves so made, under the conditions of running experienced in the sub-station in question. The wearing depth of the commutator shall not be less than $\frac{3}{4}$ inch. **Commutator.**

162. The brushes shall be of ordinary carbon, and the commutating conditions shall be such that good commutation can be effected without resorting to some special type of brush. A sample brush with its market price affixed shall be supplied with the tender. **Brushes.**

163. A sample brush-holder shall be supplied with the tender. **Brush-holder.**

164. The generator shall run sparklessly at all loads up to 25 per cent. over load at any pressure between 460 and 500 volts. **Commutation.**

165. The Contractor shall state the drop in voltage between no load at 250 R.P.M. and full load at 246, which he proposes to give in order to run in parallel with the generators set out in Schedule I. He shall also state the rise in voltage which will occur when load is thrown off. This change in voltage shall not be more than is necessary for parallel operation, as it is desired to obtain the best possible regulation on the sub-station. **Regulation.***

* Where the generators in the sub-station are compound-wound, particulars should be given of the actual rise in voltage between no load and full load on the sub-station. It should be stated whether the series coils are to be connected on the positive or negative side of the generator, and particulars should be given of the actual voltage between the equaliser bar and the positive or negative bar, as the case may be, with full load on the sub-station : that is to say, of the voltage upon the series windings of the generators at present installed.

Efficiency. 166. The efficiency shall be determined from measurements of the separate losses. The iron loss at 500 volts, the friction with all brushes adjusted for their working pressure, and the windage, shall be measured at no load. The resistances of the armature and commutating winding shall be measured at a known temperature; and the I^2R loss calculated at 60° C. The drop in the brushes shall be taken to be 2·3 volts for the purpose of calculating the brush losses. The field and rheostat losses shall be taken as together equal to the product of the amperes of field current at full load at 500 volts into the voltage. All the above losses, expressed in kilowatts, shall be added to the kilowatt output, and the ratio of the output to this sum shall be taken as the calculated efficiency. The Contractor shall state in the Schedule attached the efficiency of his generator calculated in this way at full, three-quarter and half load at 500 volts, and he shall guarantee that there shall be nothing in the construction of the machine that will lower the actual efficiency when running on load by more than 1·5 per cent. below the figures so given.

Rheostat. 167. A field rheostat with multi-contact switch is to be provided in the field circuit of the generator, of sufficient capacity to lower the voltage of the armature to 460 volts at no load when the machine is cold, and to enable the voltage to be raised to 500 volts when the generator is delivering 1250 K.W. in the hottest weather. Sufficient contacts must be provided on the switch to make the voltage change very gradually as the switch is moved over the whole range. One step of the rheostat must not change the voltage by more than 1·5 volts at any load and at any part of the range when the machine is operating by itself.

Tests before Shipment. 168. The following tests shall be carried out at the maker's works before shipment:

Of Resistances. (*a*) Measurements shall be made of the resistance of the armature and field windings.

Magnetization Curve. (*b*) The generator shall be run at full speed, no load,* with the field excited, and measurements shall be taken

* In some cases, where it is impossible to carry out a full-load test, the Purchaser may require to have a full-current commutation test on short circuit. The clause calling for this can be worded as follows:

Short Circuit. (*b'*). The generator shall be run at full speed with the armature short-circuited through the commutating poles and an ampere-meter, the field windings being excited so as to give full-load current; the machine shall, under these conditions, commutate well. Measurements shall be taken of the temperature rise of the commutator and armature after six hours' run.

showing the relations between field current and voltage generated, the iron loss at various voltages, and the friction and windage. **Iron Loss.**

(*c*) The generator shall be run at full field-current for six hours, and measurements taken of the field resistance while hot. **Field Heating Run.**

(*d*) While the machine is still hot, an alternating pressure of 1500 volts (virtual) shall be applied between the armature winding and frame for one minute. **Puncture Test.**

The following tests shall be carried out after erection on the site aforesaid: **Tests after Erection.**

(*e*) After erection on site, the generator shall be run at full load for six hours, and for two hours on the stated overload; and measurements shall be taken of the temperature of the armature, the windings and iron, and the field windings, by thermometer, and of the field windings by resistance, to see that the specified temperature rises above the surrounding air are not exceeded. For the purpose of these tests, the temperature of the room shall be taken three feet away from the generator in line with the shaft. **Temperature Run.**

(*f*) A test shall be made to ascertain the drop in voltage between no load and full load, the speed at no load being approximately 250 R.P.M., and the speed at full load being 246 R.P.M. Tests shall also be taken to ascertain the rise in voltage between full load at 246 R.P.M. and no load at 250 R.P.M., in order to ascertain whether the guarantees given by the Contractor have been met. **Regulation.**

(*g*) The generator shall be run on its ordinary daily load for one week under the direction of the Contractor's engineer, to see that all matters are in order. It need not be accepted by the purchaser until it is complete in every particular. **Endurance Test.**

169. The Tenderer shall quote separate prices for the following spare parts: **Spares.**

(1) A field coil.
(2) Twelve armature coils.
(3) One set of brushes.
(4) Enough brush-holders to complete one brush arm.

Date 5th Decr 1913. Type MG CC GEN. ~~SYN MOTOR ROTARY~~ 12 Poles ...... Elec. Spec. 11
K.W. 1000; P.F. ......; Phase ...; Volts 460–500; Amps per ter. 2000; Cycles 25; R.P.M. 246; Rotor Amps ......;
H.P. ...... Amps p. cond. 166 Amps p. br. arm. 333 Temp. rise 45°C Regulation Shunt Wd. Overload 25% 2 hrs

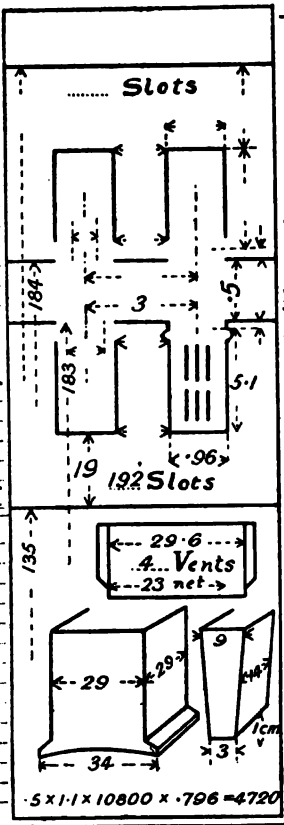
Customer ......; Order No. ......; Quot. No. ......; Perf. Spec. ......; Fly-wheel effect

Frame 183/29·5 Circum. 575; Gap Area 17000; poss $A_g B$ ......; poss. $I_a Z_a$ ......; $I_a Z_a$ / Circum. 334; $\frac{D^2 L \times RPM}{K.V.A.}$ 2·4 × 10⁵
Air ...... $A_g B$ 1·83 × 10⁸; $I_a Z_a$ 192000;

K_e ·71 ; 510 Volts = ·71 × 4·1 × ·96 × 1·83 ; Arm. A.T. p. pole 8000 ...... Max. Fld. A.T. 9500

Armature. Rev. ~~Stat.~~

Core.	Dia. Outs.	183	
	Dia. Ins.	135	
	Gross Length	29·6	
	Air Vents 4 ·9	3·6	
	Opening Min. ...... Mean		
	Air Velocity	23·5 m.p.sec	
	Net Length 26 × ·89	23	
	Depth b. Slots	19	
	Section 450 Vol.	218000	
	Flux Density	12,000	
	Loss ·028 p. cu. cm. Total	6000	
	Buried Cu. 5300 Total	9500	14800
	Gap Area 17000; Wts	5100	
	Vent Area 84000 Wts	9300	
	Outs. Area 16,000; Wts	2400	16,800
Teeth.	No of Segs 12 Mn. Circ.	555	
	No of Slots 192 × ·96 =	184	
	K_s 1·83	371	
	Section Teeth	8540	
	Volume Teeth	43,500	
	Flux Density	21,400	21,000
	Loss ·08 p. cu cm. Total	3500	
	Weight of Iron	2080	kilogrs.
Conductors.	Star or Mesh ...... Throw	1 – 16	
	Cond. p Slot	6	
	Total Conds 96 in series	1152	
	Size of Cond. ·2 × 1·9	·37	
	Amp. p. sq. cm	455	
	Length in Slots 30		
	Length outside 68 Sum		
	Total Length 98	1130 m	
	Wt. of 1,000 m 334 Total	376 kgs.	
	Res. p. 1,000 m ·455 Total	·515/144	·0036
	Watts p. m. 90		
	Surface p. m. 980 sq. cm.		
	Watts p. Sq.	·092	
	·092 × ·16	12°C	
	·0012		



Field Stat ~~or Rotor~~.

Dia. Bore	184	
½ Total Air Gap	·5	
Gap Co-eff. K_g	1·1	
Pole Pitch 48 Pole Arc	34	
K_f	·71	
Flux per Pole 10·8 × 10⁶		
Leakage n.l. ...... f.l. 1·7	12·5 × 10⁶	
Area 790 Flux density	15900	
Unbalanced Pull		
No. of Seg. ...... Mn. Circ.		
No. of Slots ...... × · =		
Vents		
K_s ...... Section		
Weight of Iron in poles	2880	

	Shunt.	Series.	Comm.
A.T. p Pole n. Load	5740		
A.T. p. Pole f. Load	9000		12000
Surface	86,000		31200
Surface p. Watt	16 sq. cm.		10·5
I². R.	5400		3000
I. R.	360		1·5
Amps.	15		2000
No. of Turns	600		6
Mean l. Turn	1·32		53
Total Length	9500		39
Resistance	19·7 (cold) 24·6 (hot)		·00075
Res. per 1,000	2·08		·017
Size of Cond.	·082 sq. cm.		10 sq. cms.
Conds. per Slot			
Total Length			
Wt. per 1,000	73		8900
Total Wt.	700	kilogrs.	350 kgs.
Watts per Sq.	·062		
Star or Mesh			
Paths in parallel			

Magnetization Curve.	Section	Length	460 Volts. B.	A.T. p. cm	A.T.	510 Volts. B.	A.T. p. cm	A.T.	540 Volts. B.	A.T. p. cm	A.T.
Core											
Stator Teeth											
Rotor Teeth	8540	5·1	19000	160	820	21,000	390	2000	22200	480	2450
Gap	17000	·5			4250			4720			5000
Pole Body	790	30	14300	17	510	15900	30	900	16800	50	1500
Yoke	710	23	8000	7	160	8800	8	184	9300	9	210
					5740			7804			9160

Commutator.

Dia. 107 Speed 1·77 m/sec
Bars 576
Volts p. Bar 14·7
Brs. p. Arm 6
Size of Brs. 2 × 4·5
Amps p. sq. cm. 6·2
Brush Loss 4600+3000
Watts p. Sq.

EFFICIENCY	1¼ load.	Full.	¾	½	¼
Friction and W.	9	9	9	9	9
Iron Loss	11	10·5	10	9·5	9
Field Loss	8	7·5	7	6·5	5·5
Arm &c I²R.	34	21·0	11·5	5	1·2
Brush Loss	5·8	4·6	3·5	2·3	1·3
	67·8	52·6	41·0	32·3	26·0
Output	1250	1000	750	500	250
Input	1318	1053	791	532	276
Efficiency	94·8	95	94·8	93·9	90·5

Mag. Cur. Loss Cur.
Perm. ~~Stat. Slot~~ Ends 1·74
" Rot. Slot × = 1·73
" Zig-zag ·94
2 × × = 4·41
1·77 × =
End × × =
Amps; Tot
τ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

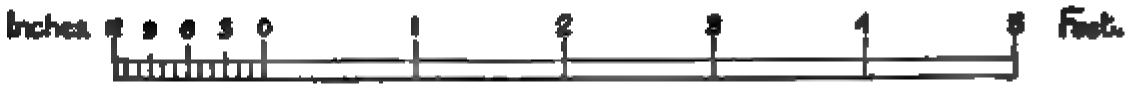
THE DESIGN OF A 1000-K.W. C.C. GENERATOR TO MEET SPECIFICATION NO. 11.

460-500 volts; 2000 amperes; 246 R.P.M.

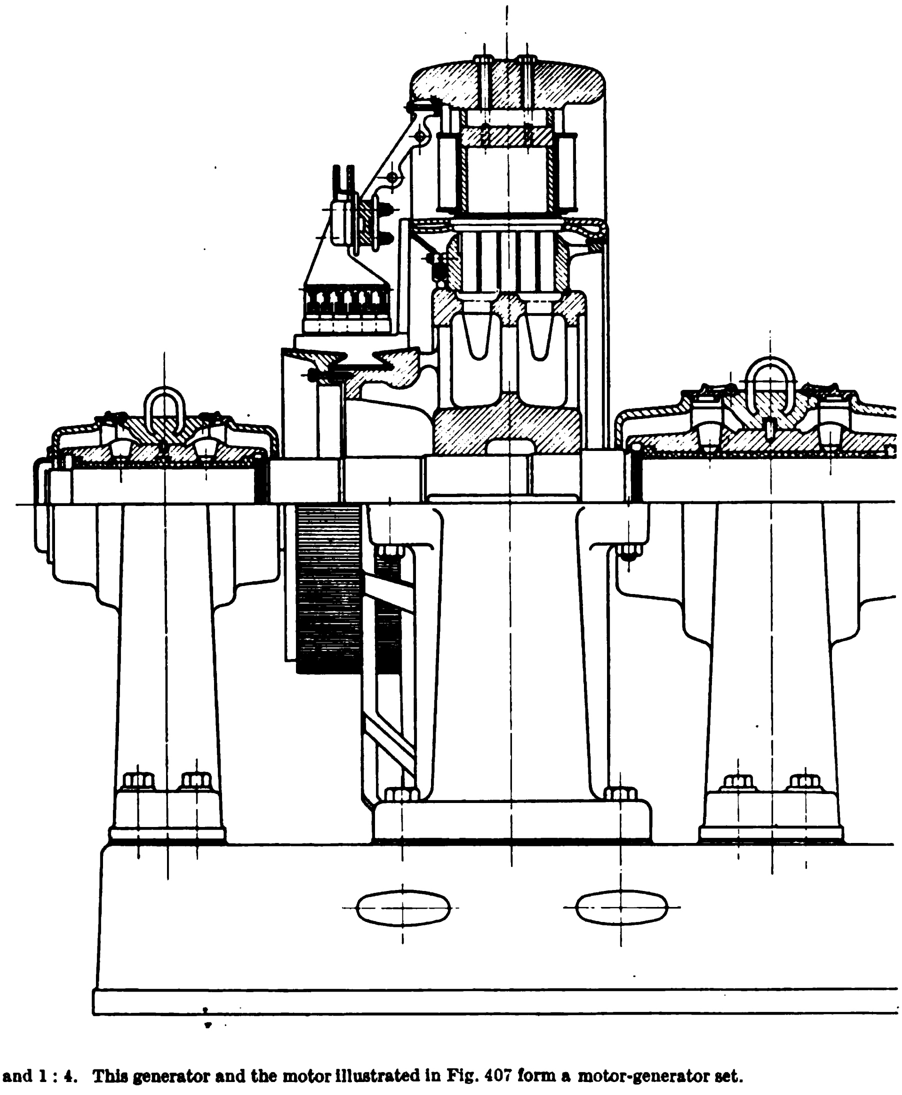
The procedure in designing this machine follows very closely that adopted for the small machine given on page 488. The D^2l constant for large multipolar C.C. generators fitted with commutating poles is, however, much smaller than for little four-pole machines. It will be found that a D^2l constant of $2{\cdot}4 \times 10^5$ will give us a frame not too small to meet the temperature guarantee. The calculation sheet is given on page 504.

The choice of the number of poles is a matter of importance. The considerations which settle the number of poles are as follows: Where the current to be delivered is very great, the number of brush-arms will be increased until the current per brush-arm is not excessive. Thus, in generators for electrolytic work, delivering many thousands of amperes at a low voltage, one may take 1000 amperes per brush-arm as a suitable figure, and fix the number of poles accordingly. When the voltage is higher, say 250 volts, a rather lower current per brush-arm will generally be chosen, from 500 to 750. On 500-volt machines it is usual to choose a still lower current per brush-arm, say from 300 amperes for machines of 500 K.W. capacity, up to 500 amperes for very large generators. It is often worth while to work out two or three designs with varying numbers of poles to see which arrangement makes the cheapest good machine on the available frames. In this case we have to deliver 2000 amperes, so that a twelve-pole machine would have 333 amperes per brush-arm. No advantage is to be gained by reducing the number of poles, as this would only increase the length of the commutator and the axial length of the iron. In this respect a C.C. generator whose speed is prescribed differs from a rotary converter, in which the diminution in the number of poles increases the speed and brings about a saving in the material. In actual practice, the diameter would be fixed by the diameter of some frame which the manufacturer might have developed; but if we were starting *de novo* we should have to make a compromise between building a machine of large diameter and short axial length, which would give us good commutating conditions, and building a machine of smaller diameter and greater axial length, which, though economical in material, might give us an excessive inductance in the armature coils. A happy mean is generally to be found in making the pole of the generator approximately square in section, or, as is sometimes preferred, somewhat longer in an axial direction than in a circumferential direction. In this case, if we take a square pole 29 cms. × 29 cms., we find that the diameter is just great enough to enable us to get in the requisite number of conductors.

The number of conductors is controlled by the number of commutator bars which we wish to have per pole. From the considerations given on page 532, we will decide on 48 bars per pole; so that in a lap winding we have 96 conductors in series. On these large multipolar machines fitted with commutating poles there is no difficulty in obtaining a coefficient K_e (see page 13) as high as 0·71. Adopting



FIGS. 432 and 433.—Sectional views of 1000 K.W. C.C. generator, 500 volts, 246 R.P.M. Scales 1 : 24



and 1 : 4. This generator and the motor illustrated in Fig. 407 form a motor-generator set.

this coefficient, and allowing 10 volts for drop of voltage in the armature, we find the value of A_gB from the equation

$$510 = 0{\cdot}71 \times 4{\cdot}1 \times 96 \times A_g\text{B}.$$

Hence $A_g\text{B} = 1{\cdot}83 \times 10^8$.

If we take a diameter of 183 cms. and an axial length of 29·6 cms., we have a circumference of 575 cms. and an area of gap of 17,000 sq. cms. We make a check calculation at this point by dividing A_gB by the gap area to see that B is somewhere in the vicinity of 10,000 C.G.S. lines. In this case B in the gap will equal 10,750, which is not too high a value for a C.C. machine if we can give enough area to the section of the teeth. We must also make a check calculation to ascertain the ampere-wires per cm. of periphery. The total ampere-wires I_aZ_a will equal 192,000, and

$$\frac{I_aZ_a}{\text{circumference}} = 334.$$

This is a suitable figure for a large machine, and will permit us to work the copper at approximately 450 amperes per sq. cm.

The amperes per conductor are 166. The cross-section of the conductor may be taken as 0·375 sq. cm., and we may fix on a copper strap 0·2 × 1·9 cms. This, with insulation and suitable room for the retaining wedge (see Fig. 158), will require a slot 0·96 cm. wide × 5·1 cms. deep.

The machine is illustrated in Figs. 432 and 433.

The next step is to check the saturation in the teeth.

$$\pi(183 - 7) = 555.$$

This gives us the mean circumference of the circle through the teeth. Subtracting from this 184, the width of all the slots, we get 371, the width of all the teeth. The net length will be 23 cms., giving us a cross-section of all the teeth of 8540. The apparent flux-density will therefore be $1{\cdot}83 \times 10^8 \div 8540 = 21{,}400$. From Fig. 46 we see that the actual flux-density will be 21,000. As the frequency is only 25 cycles, the loss (see page 52) will be 0·08 watt per cu. cm., giving 3·5 K.W. loss in the teeth. The loss behind the slots and the buried copper loss are calculated in the same manner as described on page 323. We find that the total watts dissipated by the iron surfaces of the armature are 14,800. With a 45° C. rise we see, from the calculation given in the sheet, that we can dissipate 16,800 watts. The calculation of the watts per sq. cm. on the surface of the armature coils gives us 12° C. difference of temperature between copper and iron.

Magnetization curve. It is convenient to work out the ampere-turns per pole at 460, 510 and 540 volts, as shown in the calculation sheet. It will be seen that, owing to the high saturation of the teeth, the ampere-turns on the teeth at 510 volts are 2000. The length of the air-gap will be adjusted so as to make the shunt ampere-turns on the pole somewhere about equal to the armature ampere-turns per pole. In this case, an air-gap of 0·5 cm. gives us 7804 ampere-turns per pole, which is sufficiently near 8000 to prevent undue field distortion. At full load we must allow for some further increase in the shunt ampere-turns ; on a commutating-pole machine it is sufficient to add about 15 per cent. of the armature ampere-turns, which will give us 9000 ampere-turns per pole to be provided at full load if the

brushes are rocked slightly ahead of the neutral. In calculating the shunt winding, we first make an estimate of the total cooling surface (see page 331), which is usually taken from previous machines built on the same frame, or it can be found by trial and error. In this case we have 86,000 sq. cms.; allowing 16 sq. cms. per watt, we have a permissible loss of 5400 watts. As it is desirable to have some margin in our rheostat, we will take about 360 volts drop in the winding, so that the shunt

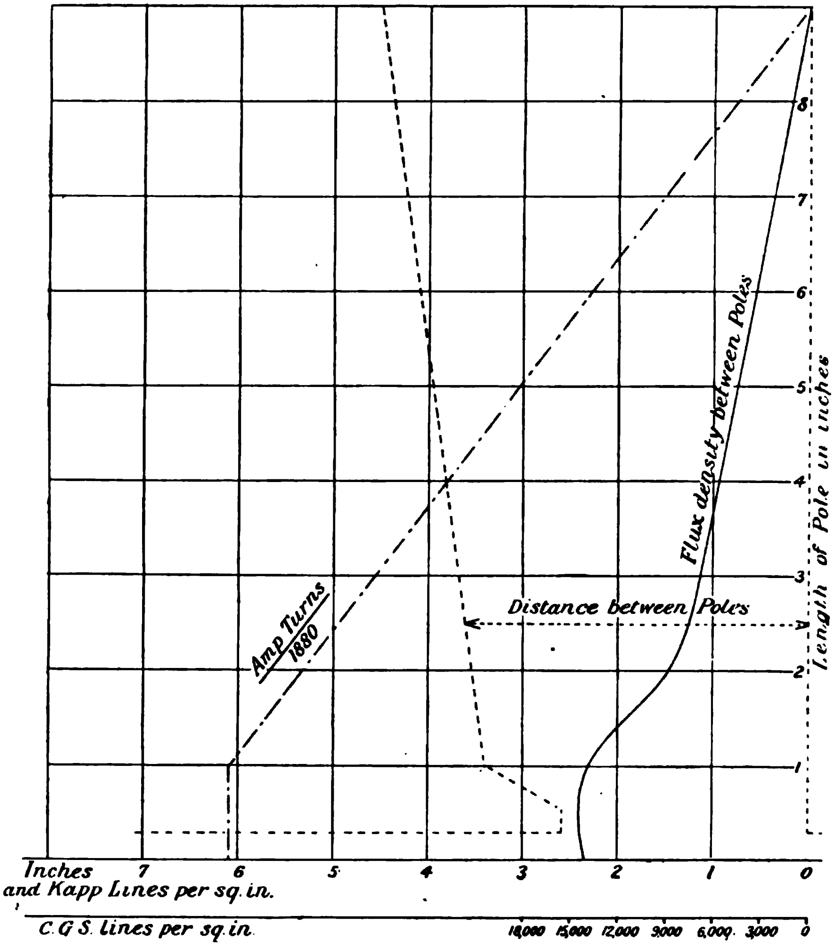


FIG. 434.—Construction for calculating leakage flux between poles of 1000 K.W. C.C. generator.

amperes will be about 15. Dividing this into 9000, we get 600 as the approximate number of turns. The main length of turn is 1·23 m., giving a total length of 9500 m. The approximate hot resistance can be obtained by dividing 360 by 15. From this and the length of wire we find the resistance per 1000 metres, and then the size of conductor, which must finally be adjusted to fit some standard size. It is then a simple matter to run over the figures and adjust them more exactly.

The leakage between poles is calculated in the same manner as described on page 326. The graphic construction is given in Fig. 434.

Commutating pole. It will be seen from the calculation sheet that the armature coil lies in slots 1 and 12; so that it is short chorded by one slot. The effect of this will be to slightly reduce the self-induction; but the main advantage lies in its giving a commutating curve of the type shown in Fig. 423, page 477. For the purpose of getting a commutation curve of this type, the width of the commutating pole must be made about the same as the pitch of the slot—in this case 3 cms. The following is the calculation of the various leakage coefficients:

$$L_n = 1{\cdot}257\left(\frac{1}{2}\,\frac{4{\cdot}3}{3\times0{\cdot}96}+\frac{0{\cdot}6}{0{\cdot}96}\right)=1{\cdot}73,$$

$$L_c = 1{\cdot}357\left(\frac{1{\cdot}5}{2}\right)=0{\cdot}94,$$

$$L_s = 0{\cdot}46\,\frac{70}{29{\cdot}5}\left(\log_{10}\frac{70}{1{\cdot}8}\right)=1{\cdot}74,$$

$$L_n + L_c + L_s = 4{\cdot}41,$$

$$B_c = 2{\cdot}8\times4{\cdot}41\times\frac{6\times166}{3+3{\cdot}4-0{\cdot}585}=2120.$$

If now we make the effective axial length of the commutating pole, 14 cms., instead of the full axial length of the armature, 29·6 cms., B_c must be increased to 4440. If the air-gap under the pole be made 1 cm. long, the effective ampere-turns upon the pole must be

$$4440\times1\times1{\cdot}1\times0{\cdot}796=3900.$$

Six turns per pole multiplied by 2000 amperes would give us 12,000 ampere-turns $=8000+4000$.

A suitable diameter of the commutator would be 107 cms.; this gives us a circumference of 335 cms. with a pitch of brushes of 28 cms. A good way of arranging the commutator V-rings and holding bolts is shown in Fig. 515. With 576 bars we have an average of 14·7 volts per bar; with 6 brushes per arm, each measuring $2\times4{\cdot}5$ cms., we get a current density of 6·2 amperes per sq. cm. If we allow 2·3 volts drop on the positive and negative brushes, we have a resistance loss of 4600 watts; and with a brush pressure of 2½ lbs. per brush we have a friction loss of 3000 watts: making a total loss in the commutator of 7600 watts. It will be seen from Fig. 433, page 507, that the commutator is provided with very long lugs, so that no difficulty will be experienced in dissipating the loss. The method of working out the efficiency will be clearly seen on the calculation sheet.

SPECIAL C.C. GENERATORS.

Before passing on to consider C.C. turbo-generators, we will take up a few matters which arise in connection with very slow-speed machines and those which for some reason cannot with advantage be built with ordinary lap windings.

We have seen that the fewer the number of turns per coil between two successive bars on the commutator, the easier are the commutating conditions. On all large machines we aim at getting only one turn per commutator bar. On small machines of ordinary voltage we are compelled to have more turns per bar, because the magnetic flux per pole is so small that we could not generate the voltage required

without having several (and in very small machines many) turns per bar. The number of bars between positive and negative brushes is limited by the fact that it is not desirable to make the bars too narrow. For instance, on a commutator 9 ins. in diameter, we would not care to have more than 200 bars, or 50 bars per pole on a four-pole generator. If now we must generate 500 volts, we have an average of 10 volts per bar, or say 15 volts maximum, and on a small generator of ordinary speed we would require several turns to generate the 15 volts.

For very small machines there is no great disadvantage in having a number of turns per coil, because the current to be commutated is small; but as we proceed to 500-volt machines of 200 to 250 K.W. capacity, the commutation with two-turn coils is not as good as we could wish, so it is better to resort to wave windings. A two-circuit wave winding on a four-pole machine gives as many conductors in series (for a given number of commutator bars) as a lap winding with two turns per coil; and although the voltage per bar is the same as for the lap winding, it has the advantage of bringing about commutation of each single-turn coil separately, and thus taking full advantage of the resistance of the carbon brush.

There are many **cases**, however, in which we **cannot with advantage** make use of the **two-circuit winding**. On machines of large size with many poles the voltage per bar, with a two-circuit winding, becomes too great, and yet it may be that a single-turn coil would not give us sufficient voltage. In these cases the **Arnold singly re-entrant multiplex** winding is most useful. The need of a winding of this kind is most commonly found in slow-speed continuous-current generators of moderate output.

In order that we may fully appreciate the use of this winding, let us take a 500 H.P. 500-volt rolling-mill motor running at 32 R.P.M. Experience leads us to a D^2l constant of 3×10^5 as suitable for a machine of this size. The armature might have a diameter of 274 cms. and an axial length of about 50 cms. As it is not economical to make the pole pitch too great, we might choose 18 poles,* giving a pole pitch of 48 cms.

Now let us see what type of winding is best for such an armature.

We may have a flux-density in the air-gap of 9500, so that the A_gB may be as high as

$$\pi \times 274 \times 50 \times 9500 = 4 \cdot 1 \times 10^8.$$

Taking K_f at 0·68 and allowing 30 volts drop in windings and brushes, we have

$$470 = 0 \cdot 68 \times 0 \cdot 533 \times Z_s \times 4 \cdot 1.$$

Z_s = about 320 conductors in series. Let us try a lap winding with as many circuits as there are poles. With only one turn per coil we would have 160 bars per pole, which is clearly too many. With two turns per coil we would have 80 bars per pole, still a large number. Three turns per coil would give us 54 bars per pole, a suitable number; but three turns per coil would not give us ideal commutating conditions.

* In this case the number of poles is not fixed by the amperes per brush arm, but rather by the circumstance that the machine is very large on account of its slow speed, and in a large diameter many poles call for less material than fewer poles. At the same time, the small current per brush arm does make the commutating conditions better than they otherwise would be, and the small number of brushes per arm enables a narrow commutator to be used.

Let us try the ordinary two-circuit winding. This would give us only 320 bars on the whole commutator, or only 17·8 bars per pole. Moreover, the current per conductor would be 415 amperes. The two-circuit winding is then out of the question. Now try an Arnold multiplex singly re-entrant winding.

We will employ the following symbols :

$2p$ = Number of poles.

$2a$ = Number of armature circuits in parallel.

K_m = Number of commutator bars.

y = Throw on the commutator—that is, the number of bars between one positive brush and the next positive brush.

N_s = Number of slots in the armature.

Then the quantities must fulfil the following conditions :

p must be a simple multiple of a.

N_s must be a simple multiple of a.

$K_m = (y \times p) \pm a$.

y and K_m must be prime to one another if the winding is to be singly re-entrant.

Further, if we are given the number of blank stampings forming a circle in the armature, N_s must be a simple multiple of the number of blanks.

Let us see how we can fulfil these conditions in the machine in question.

$$2p = 18,$$

$$I_t \text{ the terminal amperes} = 830.$$

In choosing the number of parallel circuits we aim at making the current per conductor somewhere between 125 and 250 amperes per conductor. If we divide 830 by 4 we would get 207 amperes, a suitable number for the amperes per conductor, but 18 is not divisible by 4. We therefore try $a = 3$, $2a = 6$.

$$\frac{828}{6} = 138 \text{ amperes per conductor.}$$

This is quite suitable. $\qquad 2a \times 3 = 18 = 2p.$

Next, we have to settle on the number of conductors. This we can do by adopting an economical number of ampere-wires per cm. of periphery. This should be between 250 and 360. Assume 300.

The circumference of the armature is 860 cms., and the current per conductor 138 amperes. Therefore a suitable number of conductors would be about

$$\frac{860 \times 300}{138} = 1870.$$

And K_m is half this number, or about 935.

Now apply the formula

$$K_m = y \times p \pm a. \qquad p = 9 \text{ and } a = 3.$$

Try $y = 107$, a prime number.

$$K_m = (107 \times 9) \pm 3 = 966 \text{ or } 960.$$

960 is a more promising number than 966, because it will give us an even number of slots. With 960 commutator bars we could have 240 slots with four bars per

slot; moreover, 240 slots is a likely number for fitting a possible number of blank stampings per circle.

Now it will be seen that with 960 bars we satisfy all the conditions set out on page 512.

$$K_m = (107 \times 9) - 3 = 960,$$

$$\frac{960}{18} = 53 \cdot 3 \text{ bars per pole,}$$

$$a \times 3 = 9 = p,$$

$$a \times 80 = N_s = 240.$$

107 and 960 are prime to one another, so we will have a singly re-entrant winding with six circuits in parallel.*

The total number of conductors on the armature is 1920, so there are 320 conductors in series. The last-given method of finding a suitable number of conductors will not necessarily give us the same number as we found on page 511 by considering the total flux of the frame and the speed; but it will give us a number somewhere near it, because the D^2l constant is based on our working the frame at an A_gB somewhere about $4 \cdot 1 \times 10^8$, and the amperes per centimetre of periphery somewhere about 300.

Even in cases where we could make a passably good machine with a lap winding, it will often be better to use Arnold's winding for the purpose of reducing the total number of conductors on the armature. By reducing the number of paths in parallel and increasing the current per conductor, and hence the size of the conductor, we can save insulation space and make an arrangement in which the commutating conditions are very good.

Take the case of a 200-K.W. 250-volt generator which has to run at the low speed of 180 R.P.M. With a D^2l constant of 3×10^5, we might choose a diameter of 92 cms. and length of 40 cms. Eight poles would be suitable for a machine of this size. Although we wish to generate only 250 volts, it will be found that a lap winding will require about 124 conductors per pole, or $8 \times 124 = 982$ conductors in all; each carrying 100 amperes. Now we know that 200 amperes per conductor would cut down the insulation space and give us a cheaper machine. This is possible with a multiplex winding. Take in this case $2a = 4$, because there are 8 poles. We want about 124 conductors in series, or about $496 \div 2 = 248$ commutator bars. Take the formula

$$K_m = (y \times p) \pm a,$$

and find a suitable y. Try $y = 61$.

$$K_m = (61 \times 4) + 2 = 246.$$

This number of commutator bars would allow us to have 82 slots with 3 bars per slot. As the armature stamping is made in one piece on an armature of this size, 82 slots is permissible. We thus have about 10 slots per pole, a sufficiently great and yet an economical number.

In the calculation sheet given on page 514 the machine has been worked out.

We give below enough of the winding table of this machine to show how it goes and to indicate the bars to which the balancing rings are connected. There are eleven balancing rings, each connected to two points of the windings. Where

* The reader is referred to the latter part of the paper by Dr. S. P. Smith and R. S. H. Boulding, *Journ. I.E.E.*, vol. 53, p. 232.

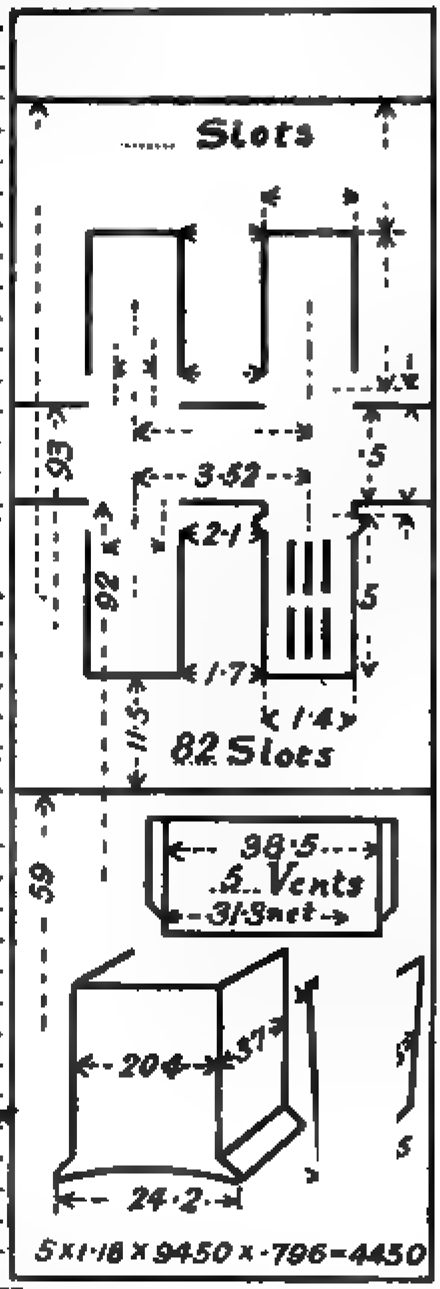
Date 5th July 1913 Type C.C. GEN. ~~SYN MOTOR ROTARY~~ 8 Poles ...... Elec Spec 12
KW 200 ; PF ... , Phase .. ; Volts 250 ; Amps per ter 800 , Cycles 12 , RPM 180 ; Rotor Amps
HP .... Amps p. cond. 200 Amps p. br arm. 200 Temp rise 45°C Regulation Level Comp Overload 10% 2hrs

Customer .... , Order No. .... , Quot. No. .... ; Perf. Spec .... , Fly-wheel effect

Frame 92/38.5 Air Circum. 290 ; Gap Area 1150 ; poss $A_g B$ ... $A_g B$ 1·05 × 10⁸ ; poss $I_a Z_a$ ... $I_a Z_a$ 98500 ; $\frac{I_a Z_a}{\text{Circum.}}$ 340 , $\frac{D^2 \cdot L \cdot RPM}{K.V.A.}$ 2·9 × 10⁵

K_a 68 , 264 Volts = ·68 . 3 × 123 × 1·05 , Arm. A.T. p. pole 6150 , Max. Fld. A T 6500

Armature	Rev. ~~Stat.~~		
Core	Dia. Outs.	92	
	Dia. Ins.	59	
	Gross Length	38·5	
	Air Vents 5; 0·65	3·25	
	Opening Min. ... Mean		
	~~Air~~ Velocity	8·7 m	p. sec.
	Net Length 35·25 × ·89	31·3	
	Depth b. Slots	11·5	
	Section 360 Vol.	79,000	
	Flux Density	12500	
	Loss ·01 p. cu Cm Total	790	
	Buried Cu 2700 Total	1400	4190
	Gap Area 11,150 Wts	1800	
	Vent Area 31,000 Wts	930	
	Outs. Area 12,000 Wts	1800	4530
Teeth	No of Segs 1 Mn Circ	270	
	No of Slots 82 × 1·4 =	115	
	K_a 2·1	153	
	Section Teeth	4900	
	Volume Teeth	24,500	
	Flux Density	21,400	21,000
	Loss ·025 p cu Cm Total	610	
	Weight of Iron	810 kgs	
Conductors	Star or Mesh ... Throw	1–9	
	Cond. p Slot	6	
	Total Conds	492	
	Size of Cond. ·32 × 1·9	·6 sq. cm.	
	Amp. p sq.	332	
	Length in Slots 39		
	Length outside 50 Sum	89	
	Total Length	440 m.	
	Wt of 1,000 590 Total	260 kgs.	
	Res p 1,000 ·284 Total	·125/16	·0095 ho
	Watts p. metre 82		
	Surface p. metre	1100	
	Watts p Sq. Cm.	·075	



Field Stat ~~or Rotor~~				
~~Dia.~~ Bore	93			
½ Total Air Gap	·5			
Gap Co-eff. K_g	1·18			
Pole Pitch 36·2 Pole Arc	24·2			
K_f	·68			
Flux per Pole 9 × 10⁶	10·2 × 10⁶			
Leakage n.l. 1·2 f.l. 1·8	10·8 × 10⁶			
Area 740 Flux density	14,600			
Unbalanced Pull				
No. of Seg. ... Mn. Circ				
No. of Slots ... × =				
Vents				
K_s ... Section				
Weight of Iron in Poles	1400 kgs			
	Shunt.	Series.	Comm.	
A.T. p Pole n. Load	5700			
A.T. p. Pole f. Load	5700	3190	7960	
Surface	52,000			
Surface p. Watt	23·6			
I^2 R.	2200			
I R	176			
Amps.	12·5			
No. of Turns	460	4	10	
Mean l. Turn	134	140	54	
Total Length	4700	540	515	
Resistance 12·3	14	·002	·0018 hr	
Res. per 1,000	2·58			
Size of Cond. ·29 rd ·066		4·5	4·7 sq. cm	
Conds. per Slot				
Total				
Length				
Wt. per 1,000				
Total Wt.	300 kg.	73 kg.	86 kg.	
Watts per Sq.	·0425			
Star or Mesh				
Paths in parallel				

Magnetization Curve	Section.	Length	B. (Volts)	A.T.p. (Volts)	A.T. (Volts)	B. (250 Volts)	A.T.p.cm (250 Volts)	A.T. (250 Volts)	B. (264 Volts)	A.T.p.cm (264 Volts)	A.T. (264 Volts)
Core	360	12									
Stator Teeth											
Rotor Teeth	4900	5				19700	190	950	21,000	320	1600
Gap	11,500	·5				8900		4200	9450		4450
Pole Body	740	30				13700	12	360	14,600	20	600
Yoke	580	33				8700	5	170	9300	6	200
								5680			6850

Commutator	
Dia. 62	Speed 5·8 m. sec.
Bars	246
Volts p. Bar	7·2
Brs. p. Arm	4
Size of Brs	2 × 4·5
Amps p sq	cm
Brush Loss	500 + 1800
Watts p. Sq.	·55

Efficiency.	1¼ load.	Full.	¾	½	¼
Friction and W		750			
Iron Loss		1350			
Field Loss		2200			
Arm. &c. I²R		8500			
Brush Loss		1800			
		14,600			
Output		200			
Input		215			
Efficiency %		93			

Mag. Cur. Loss Cur
Perm. Stat. Slot
.. Rot. Slot × =
.. Zig-zag
2 × × =
1·77 × × =
End × × =
Amps; Tot
r = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

Armature	·0095
Series Fld.	·002
Comm.	·0018
	·0133

$a=2$, there will be two points of equal potential. Beginning at bar 1, we traverse one-half of the total conductors before we come to a point of the same potential as bar 1. This is bar 124. Similarly bar 180 is cross-connected to bar 57, which is just half-way through the total number of steps from bar 180.

TABLE XX. WINDING TABLE OF 200 K.W. GENERATOR WITH ARNOLD MULTIPLEX SINGLY RE-ENTRANT WINDING.

$2p=8$; $2a=4$; $y=61$; $K_m=246$.

The numbers in the Table refer to the numbers of the Commutator Bars. Where two numbers appear side by side, as 1-124, there is an equalizer connection between those two numbers.

1-124	62	123	184	211	26	87	148
245	60	121	182	209	24-147	85	146
243	58	119	180-57	207	22	83	144
241	56	117	178	205	20	81	142
239	54	115	176	203-80	18	79	140
237	52	113-236	174	201	16	77	138
235	50	111	172	199	14	75	136-13
233	48	109	170	197	12	73	134
231	46-169	107	168	195	10	71	132
229	44	105	166	193	8	69-192	130
227	42	103	164	191	6	67	128
225-102	40	101	162	189	4	65	126
223	38	99	160	187	2	63	124
221	36	97	158-35	185	246	61	122
219	34	95	156	183	244	59	120
217	32	93	154	181	242	57	118
215	30	91-214	152	179	240	55	116
213	28	89	150	etc.	etc.	etc.	etc.

TABLE XXI. SHOWING ARRANGEMENT OF EQUALIZING CONNECTIONS ON ARNOLD MULTIPLEX SINGLY RE-ENTRANT WINDING. ELEVEN EQUALIZER RINGS.

Ring No.	I.	II.	III.	IV.	V.	VI.	VII.	VIII.	IX.	X.	XI.
Commutator bar	1	13	24	35	46	57	69	80	91	102	113
Commutator bar	124	136	147	158	169	180	192	203	214	225	236

In going through the calculation sheet there are one or two points that arise on this slow-speed machine. It will be seen that the iron loss is extremely low, on account of the low frequency. The teeth are worked at $B=21{,}400$, and yet the iron loss is less than one-quarter of the armature copper loss. It is well that it is so, because the total surfaces of the armature iron and winding are not able to dissipate very much more than the 7450 watts lost, and of this 6100 is armature copper loss.

The saturation of the teeth is so high that it is well to calculate K_s (see page 71), and to correct the apparent flux-density 21,400 to 21,000 by means of Fig. 46.

The copper is worked at only 332 amperes per sq. cm. Even at this current density, the end windings should be well opened out so that the air can get between individual coils.

THE SPECIFICATION OF C.C. TURBO-GENERATORS.

There is a considerable demand on the market for continuous-current generators directly connected to steam turbines, particularly where the output is not greater than 1000 K.W. For larger outputs there is a good deal to be said in favour of employing an A.C. generator connected to a rotary converter. The loss on the converter, which may amount to about 4 per cent., can be saved in the higher efficiency of the high-speed A.C. generator set. Another solution where a steam turbine is to be used to generate continuous current is to drive an ordinary slow-speed generator by means of a double helical gear.

How far the makers of high-speed C.C. generators for direct connection to the turbines will hold the field will depend upon their success on making thoroughly reliable generators to run at speeds that are quite suitable for the designers of the steam turbine. So many successful machines are now running that there appears to be no doubt that, for small sizes at any rate, the direct-connected generator will continue to hold the field.

The main difficulties which have occurred in the past with high-speed C.C. generators are the following:

Changing of the running centre. It has been found almost impossible to build a machine which would permanently retain its balance with great accuracy. The insulation on the conductors will always shrink a little, causing sufficient movement of the conductors to disturb the balance; so that, however carefully a machine is built, it will be found that from time to time the balance has altered just a very little, and the brushes in consequence do not operate well.

Contact between commutator and brushes. It is, of course, important at high speeds that the commutator shall be perfectly round and run in a true circle, in order that the carbon brushes may keep in perfectly close contact. It is difficult to keep a commutator as true as one would like it to be for these high speeds.

Carbon brushes. For a long time metal wire brushes were used to overcome the difficulty of keeping contact; but metal brushes cause too great a wear on the copper of the commutator, and are themselves worn away too fast to give satisfactory operation. It is now generally conceded that to be entirely satisfactory, a C.C. generator must be fitted with carbon brushes.

Radial commutator. The plan of employing a commutator, the working faces of which form planes at right angles to the axis of rotation, has very much simplified the problem of keeping perfect contact at very high speeds. Any small deficiency in the balance will not cause the surface of the commutator to throw off the brush. Certain difficulties were at first encountered in the construction of these radial-faced commutators, as the expansion and contraction of the metal would sometimes distort the radial face out of the true plane; but more recent constructions have overcome the difficulty, and radial-type commutators can now be built to collect several thousand amperes up to speeds of 3000 R.P.M.; and even where the want of balance is quite perceptible on the bearing pedestals, there is not enough motion at right angles with the face of the brush to interfere with the electrical contact. The radial commutator machines are now very widely used for marine work, for which they are particularly suited, on account of the small amount of attention required.

Diameter and length. One difficulty which has been experienced in designing c.c. turbo-generators of large output and high speed arises from the fact that the diameter of the armature is limited by mechanical considerations; and the only way of increasing the output is by increasing the length. With a great length of armature iron, the voltage per turn generated in the armature coils becomes so great that the commutation becomes somewhat sensitive. The importance of having a low voltage per bar is considered on page 532. In order to overcome this difficulty, several devices have been employed: one of these is to wind a ring armature so that the voltage per turn is only one-half what it would be on a drum-wound armature; another device is to connect the back of the armature winding to alternate commutator bars by means of conductors carried between the armature iron and the shaft, as illustrated in Fig. 438.* A third method is that illustrated in Fig. 435. Here the armature iron is divided into two sections, each of which may be regarded as an independent armature of half the length. A main winding, consisting of conductors of sufficient section to carry the full current, embraces both sections of the iron, and would by itself constitute a winding having half the desired number of commutator bars per pole. Before this main winding is put into the slots, a number

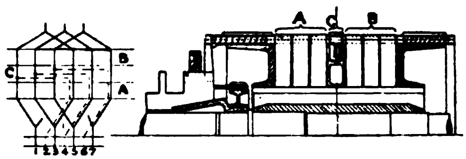


FIG. 435.—Auxiliary connectors to intermediate commutator bars. The odd bars 1, 3, 5, etc., are connected in the ordinary way to the armature winding. The even bars 2, 4, 6, etc., are connected to points on the winding by the connectors (shown dotted), which only embrace the iron of section *A*.

of auxiliary connectors are placed in the bottom of the slots, and these are connected to the main winding and to alternate commutator bars in such a way that as we pass from an odd bar to an even bar through the main winding and the auxiliary winding, we embrace the iron of only one section of the armature; and as we pass from an even bar to an odd bar, we embrace only the other section of the armature iron. Thus the voltage per bar is only one-half of what it would be if the main winding were used alone. The advantage of this method over the method shown in Fig. 438 is that, with it, the self-induction of the auxiliary connectors is given the same value as the self-induction of the main conductors, and both pass under the commutating pole in a manner which makes the commutation between odd and even bars identical with the commutation between even and odd bars. The method, however, leads to a somewhat more expensive construction than that shown in Fig. 438; and as the latter method will be quite satisfactory under the conditions obtaining in the machine there illustrated, it has not been thought worth while to adopt the more expensive construction in that case.

Distance between brush arms. Another difficulty arises in connection with the distance between brush arms. As the diameter of the commutator is necessarily restricted, we must have either very few poles or a very short distance between

* Dr. R. Pohl, "The Development of Turbo-Generators," *Journ. I.E.E.*, vol. 40, p. 239.

brush arms. If the number of poles is made too few, the current per brush arm becomes so great that the commutator becomes too long. Hence there has been a tendency on the part of designers to lower the speed, so as to enable a larger number of brush arms to be employed on machines of greater output. This diminution of the speed interferes so much with the efficiency of the steam turbine that the turbo-set can, under these conditions, no longer compete with a high-speed A.C. generator connected to a rotary converter. It is believed, however, that up to sizes of 1000 K.W. at 550 volts satisfactory C.C. turbo-generators can be built, running at 3000 R.P.M., and that such sets will be not only cheaper but more efficient than the A.C. generator and rotary converter combination. In much larger sizes, however, there is no doubt that the brush-arm difficulty prevents C.C. turbo-generators from being built for very high speeds.

Compensating winding. A small number of poles on an armature of large output necessarily entails a large number of ampere-turns per pole ; and it therefore becomes necessary to provide on these machines a compensating winding which will prevent undue armature distortion. For small sizes, say up to 300 K.W., successful machines can be built without a distributed compensation winding, the simple winding of the commutating pole being sufficient to bring about good commutation. It must not be thought that the addition of a compensation winding necessarily entails a very great expense ; because where the armature cross-magnetizing action is completely neutralized, a very much smaller air-gap can be employed, and the copper in the shunt winding can therefore be very much reduced. Moreover, every turn that is put into the compensating winding constitutes a turn on the commutating pole ; so that the turns adjacent to the commutating pole are correspondingly reduced.

Commutating poles. As the commutating interval is extremely short on these machines, and the current per brush arm is often very great, the commutating voltage is often higher than on slow-speed C.C. machines. In cases, however, where sufficient commutator bars per pole (see page 532) are employed, the commutating voltage is not too great to be satisfactorily dealt with by means of a commutating pole and carbon brushes. In fact, it cannot be said that the difficulties met with in the past have been commutating difficulties : they have rather been difficulties of collecting the current from a rapidly revolving metal surface.

Critical speed. It is found very difficult on many C.C. turbo-generators to make the shaft sufficiently stiff to give a critical speed above the running speed. This is because the bore of the spider on which the commutator is mounted restricts the diameter of the shaft. It will, therefore, be generally found that high-speed C.C. generators run above their critical speed. Where, however, the construction is sufficiently rigid and proper methods of balancing are employed, this leads to no practical difficulty, and many such machines are giving very excellent service.

Specification. In drawing up a specification for a C.C. turbo-generator, the purchaser should have in mind the difficulties which have been met with in the past ; but he should not so word his specification as to restrict the manufacturer in his methods of overcoming the difficulty. It is sufficient that he should insist that the machine put forward shall be free from the troubles met with in the past. It will be seen from the model specification given below in what way this can be achieved.

SPECIFICATION No. 13.

STEAM TURBINE CONTINUOUS-CURRENT GENERATOR SET.

170. The work covered by this specification is to be carried out in accordance with the general conditions and regulations issued by and dated the day of 19 . General Conditions.

171. It includes the supply, delivery, erection, testing, finishing and setting to work in the Corporation's Generating Station at of the following plant: Extent of Work.

Section I. One high-pressure steam turbine.

Section II. One 1000 K.W. continuous-current generator direct-connected to the steam turbine.

Section III. One surface condenser with air-pump and circulating pump of sufficient capacity for the above-mentioned turbo set, together with the motors for driving the same.

Section IV. All pipe work and valve work between the turbine and the condenser and between the condenser and its air and circulating pump.

172. The turbo set is to be erected on the site shown in the accompanying drawing No. , or as may be shown on additional drawings furnished by the engineer of the Corporation or supplied by the contractor and approved by the said engineer.

173. Any fittings, apparatus or accessories which are not enumerated in this specification, but which are usual or necessary in the equipment of such plant, are to be provided by the contractor without extra charge. Accessories not Specified.

174. The contractor is to verify all dimensions and particulars given on the said drawings, and is to obtain all necessary measurements on site.

175. As the contract for the buildings and foundations has been let and the same are in hand, the contractor will be required to arrange his plant to suit them. Drawings of Alternative Clause as to Drawings.

the buildings and foundations will be supplied for the use of the contractor, and may be seen at the offices of the Corporation for the purposes of the tender.

Work carried out by Corporation.

176. The following work will be carried out by the Corporation, and is not included in this contract:

(*a*) The erection of the power-house, including all work and materials connected with the floors, and the final floor surface (except such materials as are a necessary part of the plant).

(*b*) All work and materials required in connection with excavation and building of trenches and pits, and filling in and making good, as well as the cutting away and making good of walls for pipes, supports, etc. Cover-plates for trenches will be supplied by the Corporation, except such cover-plates as form a necessary part of the turbo-generator set.

(*c*) The installing of the circulating water-pipes up to the flanges of the circulating pumps; the installing of the main fresh water supply to the generating station, together with all meters and pipes up to the connections of the turbo-generator set, if any.

(*d*) The installing of the main supply and discharge pipe valves and the gearing for the same, in connection with the supply and discharge trenches in the engine-room basement, but not the necessary pipes and valves between these trenches and the steam-driven generating plant included in this specification.

Extent of the First and Second Sections of Specification.

177. The work covered by the first and second sections of the specification includes, in addition to the turbine and generator, all fittings, pipes and connections on the turbine and on the generator, but does not include any work beyond the inlet flanges of the turbine high-pressure steam separator, or the outlet flange of the exhaust steam stop-valve, or beyond the exhaust flange of the turbine, nor does it include any cable work or trench work beyond the terminals of the generator. The work includes the high-pressure steam separator of the turbine and a field regulating resistance and surface-plate multiple contact switch for the same.

Extent of the Third Section of Specification.

(Here follows a statement of the extent of the work on the condenser.)

Extent of Fourth Section of Specification.

(Here follows a statement of the extent of the work on the pipes and valves, etc.)

(See Clauses 6, p. 271; 36-7, p. 360; 74, p. 382; 272, p. 591.) Foundations.

(See Clauses 125, p. 461; 55-59, p. 379.) Accessibility of Site.

(See Clauses 8, p. 271; 60, p. 379; 273, p. 591.) Use of Crane.

SECTION I.

Turbine.

(This does not fall within the province of this book.)

SECTION II.

Generator.

178. The generator is to be of the compound-wound continuous-current type, having a drum armature with the winding in open slots. The machine is to be provided with compensating windings and commutating poles, and is to be suitable for supplying current for traction purposes. It shall have the characteristics set out below: *Rating and General Characteristics.*

Normal output	1000 K.W.
Voltage at full load *	600.

* Where a machine is intended for lighting as well as for traction service, the generator will be described as a compound and shunt wound generator, and the voltage characteristics may be stated as follows:

Normal voltage on traction	600 volts.
Compounding	From 575 to 600.
Amperes on traction	1670.
Normal voltage on lighting	480 volts.
Amperes on lighting	1700.
Adjustment of voltage on rheostat when machine is running as a shunt generator	460 to 500 volts.
Regulation as a shunt generator	10 per cent. drop in voltage between no load and full load, the speed being constant and the rheostat untouched;
or,	
Regulation of set, generator running as shunt machine	The speed regulation of the turbine and the inherent regulation of the generator shall be such that the voltage shall not fall by more than 16 per cent. between no load and full load.

A clause is sometimes added to the effect that the governor may be altered when running on traction so as to give 10 per cent. higher speed. This has the advantage that it enables the generator to work at the best state of saturation both on lighting and on traction.

Amperes.	1670.
Voltage variation, no load to full load	575 to 600.
Speed	Fixed by maker.
Over load	25 per cent. for 1 hour. 50 per cent. for 5 minutes.
Excitation	Shunt and series coils.
Temperature rise after 6 hours full load run	45° C. by thermometer. 55° C. by resistance.
Temperature rise after 2 hours 25 per cent. over load	60° C. by thermometer. 65° C. by resistance.
Puncture test	1500 volts alternating applied for 1 minute between copper and iron.

Horizontally split.

179. The field frame shall be split horizontally and arranged so that the armature can be lifted out without disconnecting more than a minimum number of field connections.

Critical Speed.

180. The revolving part of the generator shall be so constructed that the critical speed differs from the running speed by not less than 700 revs. per minute.

Balance.

181. The revolving armature shall be so constructed that practically no relative motion shall occur between the constituent parts after completion. The armature shall be balanced with extreme accuracy so that no perceptible vibration is communicated to the bearings; approved means shall be provided both on the commutator and on the armature for fixing balance weights and enabling the same to be readily changed in position.

Noise.

182 The generator shall be enclosed so that it shall not give rise to any more noise than would be observable in machines of similar size built according to the best practice.

Factor of Safety.

183. At the normal speed chosen by the maker the calculated factor of safety in every part shall not be less than 4. The revolving parts shall, before leaving the contractor's works, be run at a speed 10 per cent. above normal without showing any signs of movement of the component parts relatively to one another.

184. The armature is to be provided with a half coupling, and is to be driven by a half coupling supplied and fixed to the end of the turbine shaft. This coupling shall be of an approved type. **Coupling.**

(See Clauses 67, p. 380; 268, p. 590.) **Bearings.**

(See Clause 68, p. 381.) **Eddy Currents in Shaft.**

(See Clauses 24*a*, p. 334; 33 and 35, p. 360; 68-9, p. 381.) **Shaft.**

185. The generator is to be fixed to a bedplate of approved construction which shall give sufficient rigidity, having regard to the type of foundations proposed, and ensure the true alignment of the turbine and armature shafts at all times. **Bedplate.**

186. The holding-down bolts and foundation plates are to be provided by the contractor. **Holding-down Bolts.**

187. The armature shall be built up of punchings, which shall be supported on the spider or on the shaft in such a manner that there is no possibility of their becoming loose or moving even by the smallest amount relatively to the shaft. The supports for the end windings shall be such that they can be readily taken off and replaced in case it should be necessary to repair the armature windings, and the armature winding shall be of such a type that a new coil can be inserted without any great delay. **Type of Armature.**

188. The commutator and brush gear shall be designed so that there is no tendency for the brushes to be thrown off the commutator when running at a high speed, notwithstanding small errors in the balance of the armature. **Commutator and Brush Gear.**

189. The brushes shall be of carbon. **Brushes.**

190. A drawing of the proposed commutator and brush gear shall be supplied, showing the method of supporting the commutator bars and securing them against centrifugal forces, and showing also the method of making the connections between the armature conductors and the commutator segments. **Drawing of Commutator submitted with Tender.**

191. The commutator is to be built up of hard-drawn copper segments accurately spaced circumferentially, insulated from **Construction of Commutator and**

one another by built-up mica of uniform thickness ; the whole being supported on mica bushes in such a manner as to avoid radial or axial displacement. The arrangements for ventilation shall be such as to secure a continual supply of cold air playing over the cooling surfaces of the commutator. Means are to be provided for tightening-up bolts or nuts on the commutator centring bushes without the necessity of disturbing any of the armature connections, and also for controlling any movement due to the expansion and contraction of the segments. The mica bushes separating the commutator from its metal retaining bushes shall present a creeping distance of not less than 1 inch. The tenderer shall state the depth of copper allowed for wear. Particulars shall be given of the number and sizes of the brushes proposed and the mean current density in the brushes with a load of 1670 amperes on the generator. Provision is to be made for adjusting the position of the whole set of brushes and arms simultaneously by means of a worm gear or in other approved manner. The brush gear is to be designed so that all adjustment and replacement of brushes and the cleaning of the commutator can be done when the machine is running.

Brush Gear.

Sample Brush-holder. 192. A sample brush-holder shall be supplied with the tender.

Commutation. 193. The generator shall be designed to run at all loads up to full load under all conditions as to voltage regulation specified with a fixed position of the brushes, without any sparking visible at the corners of the brushes.

Throwing Load on and off. 194. It shall be possible to throw on full load and throw off 100 per cent. overload suddenly without causing flashing over the commutator.

Similar Machines in Operation. 195. The tenderer shall give in a schedule a list of places where machines of a similar character can be seen in operation. Preference will be given to the type of machine which experience has shown to require the minimum attention to brush gear during normal operation.

Ventilation. 196. If the contractor requires cool air drawn from the outside of the building to cool the machine in question, he should state the fact when tendering and show on his tender drawing how the ducts for supplying such air can be conveniently arranged.

197. The machine shall be so designed that when running as a compound generator it will run well in parallel with the continuous-current machines at present installed in the Corporation's generating station. The existing generators consist of the following sets : **Parallel Running.**

(Here follows list of existing sets.)

198. These sets at present run in parallel when over compounded from 575 volts no load to 600 volts full load. The series windings on these machines are connected between the equaliser bar and the positive bus bar. The voltage between these bars with full load on the station is 1·5 volts. The contractor must supply all the series resistances and diverters which may be necessary to make the generator supplied by him divide its load equally with the other generators in the station. Drawing No..... shows the position of the bus bars and equalising bar and the size of cables proposed for making connection to the turbo-generator.

199. The main terminals shall be designed to take conveniently the size of cable proposed by the Corporation, and they shall be fixed to the frame and insulated in a substantial manner. The field winding terminals of the generator are to be entirely separate. Each cable socket shall be provided with clamping screws and shall be clamped as well as sweated, so as to prevent the cable from falling out if by any accident it become overheated. All terminals of opposite polarity are to be arranged so that they are either at least 6 inches apart or are provided with insulating screens which make the shortest arcing distance between them not less than 6 inches. **Terminals.**

200. The contractor is to supply the spare parts set out in Schedule I., and he is also to state what other spare parts he recommends, together with their prices. **Spares.**

201. The contractor is to provide a full outfit of spanners and special tools necessary for disassembling and assembling the generator, together with a rack for holding them. **Tools.**

202. The efficiency of the generator shall be calculated from the separate losses, which shall be measured as follows : **Efficiency.**

(*a*) *Iron loss, friction and windage.* The generator shall be run at full speed at no load as a continuous current motor,

the pressure at its terminals being 600, with all brushes in position and adjusted to their working tension. The power taken to drive the generator under these conditions shall be taken as the sum of the iron loss, friction and windage.

(*b*) *Copper losses in armature and field.* The resistances of the armature and all field windings, including the compensating winding and commutating winding, with diverter if any, and the series winding with additional resistance, shall be measured by passing a substantial current through them and observing the voltage drop. The I^2R losses at full load shall then be calculated from these resistances, due allowance being made for the actual temperature rise on load. The loss in the field rheostat shall also be included.

(*c*) *Brush losses.* The brush losses shall be taken as equal to the watts obtained by multiplying the armature current by 2 volts. The contractor shall state in his tender the efficiency, calculated from the separate losses, which he guarantees at full load, three quarter load and half load; he shall also guarantee that the efficiency under actual running conditions will not be more than 1 per cent. lower than the efficiencies so calculated.

Tests at Maker's Works.

203. The following tests shall be carried out at the contractor's works in the presence of the engineer of the Corporation, before being forwarded for erection at the station:

(*a*) Iron loss, friction and windage measurement.

(See Clause 168*b*, p. 503.)

(*b*) Copper loss measurement.

(See also Clause 235, p. 564.)

(*c*) Commutation test on short-circuit. The positive and negative terminals shall be short-circuited through an amperemeter, the machine run at full speed, and the current brought up to full-load value and maintained there for 3 hours to test the commutating qualities of the machine. The machine shall not be forwarded until all adjustments have been made that shall be necessary to bring about perfect commutation.

(*d*) Puncture test. At the conclusion of the short-circuit tests when the armature is still hot, a puncture test of 1500 volts alternating shall be applied between the armature winding and frame and between the field winding and frame.

204. After erection in the station of the Corporation the following tests shall be made : Tests after Delivery.

(*a*) Temperature run. The generator shall be run for 6 hours on full normal load, viz. 1670 amperes at 600 volts, and shall then be shut down with all possible speed, and the temperature of the principal parts taken by means of a thermometer. The temperature of the shunt winding shall also be calculated from its rise in resistance. No part of the generator shall rise in temperature more than 45° C. measured by thermometer, or 50° C. measured by rise of resistance.

(*b*) Over-load run. Immediately after taking the temperatures mentioned in the last paragraph, the generator shall be run at 25 per cent. over load for 2 hours without exhibiting a rise of temperature of more than 55° C. by thermometer, of 65° C. by resistance.

(*c*) Commutation. During the full-load temperature run, the commutation shall be noted and shall not be deemed satisfactory if any sparks are visible at the edges of the brushes. On 25 per cent. over load there shall not be sufficient sparking to injure in any way the brushes or the commutator. After the full load run there shall be no apparent marking of the commutator.

(*d*) Switching in and out. Full load shall be suddenly switched on to the generator while it is going at full speed, and the circuit breaker shall be opened by having 100 per cent. over load suddenly thrown on the generator. The generator shall not flash over or be otherwise injured by this treatment.

(*e*) Paralleling test. The generator shall be run in parallel with any one or a number of the existing generators, and shall divide the load with them sufficiently well for practical purposes.

(*f*) Compounding test. The load shall be varied from no load to full load to see that the generator compounds from 575 to 600 volts.

(*g*) Absence of undue noise and vibration. It shall not be possible to hear the generator running outside the station of the Corporation, and the vibration of the pedestals and bedplate shall be not greater than is observable on the machines of the best construction.

205. In measuring the temperature rise the atmospheric temperature shall be taken by means of a thermometer Temperature Rise.

placed in the flume or duct which brings the ventilating air to the base of the machine.

Maintenance Period.

206. The satisfactory completion of the above tests shall not exonerate the contractor from liability in connection with the good running of the plant during the first six months after the set has been accepted and taken over. If during this six months defects in the commutation or any other defects due to faulty construction or design, or bad workmanship, shall become apparent, the same shall be immediately rectified by the contractor, and any time which shall elapse between the notice given to the contractor of such defect and the remedying of the same shall not be included in the six months' maintenance period.

Provision of Load and Steam.

207. The Corporation will provide the means of loading the generator for the temperature tests, and they will supply all steam and labour for such tests free of charge. They will also supply free of charge steam and labour equivalent to 6 hours' full load run to enable the contractor to adjust the plant. Any additional power which the contractor may require will be supplied to him at the cost of one halfpenny per unit. Should the first official tests not be satisfactory, they are to be repeated, and the cost of such additional tests shall be borne by the contractor.

Provision of Instruments.

208. The contractor shall provide all standard instruments necessary for the foregoing tests and pay for the calibration of the same.

Cleaning and Painting.

209. Before delivery all rough parts of the turbo-generator shall be properly filled, and it shall be given one coat of paint. After it has been erected and tested in the station, and when it is ready for continuous working, it shall be properly cleaned, and painted by the contractor with two coats of oil paint of approved colour, and one coat of varnish. If required by the Corporation, this painting may be deferred until the end of the term of maintenance.

Drawings supplied by the Corporation.

210. Drawing No.   supplied with this specification shows the existing lay-out in the generating station of the Corporation and the proposed site for the new turbo-generator set. The contractor is advised to inspect the site and make all necessary measurements. The contractor is to be responsible

for obtaining any information which shall be necessary to him in deciding as to the suitability of the site for his plant and for the exact dimensions of all foundations, pipes, flanges and clearances, and other matters with which he may be concerned.

211. Schedule No. 2 gives a list of the drawings and samples which are to be submitted with the tender. **Drawings to be supplied with Tender.**

212. A provisional sum of £100 is to be included in the total price submitted for the whole of the work, which sum will be dealt with in accordance with Clause of the General Conditions. **Provisional Sum.**

213. The tender shall state on what date the plant will be erected and ready for work. **Delivery.**

DESIGN OF A 1000-K.V. CONTINUOUS-CURRENT TURBO-GENERATOR.

As we have seen, it is desirable to settle upon as high a speed as possible, but this must at the same time be consistent with thoroughly good performance.

The main difficulties in the way of choosing very high speeds are as follows. Just as with A.C. generators, the maximum diameter that can be chosen will depend upon the speed, and the tendency among designers is to employ a somewhat smaller diameter for C.C. generators than for A.C. generators of the same speed. The reason is that the winding of a C.C. generator, being subjected to a fairly great voltage between turns, requires even greater care in its insulation and cannot be supported quite so well mechanically as the winding of the field magnet of an A.C. generator. The wedges in the tops of the slots must be made of some insulating material, such as fibre, instead of brass. Moreover, the connections to the commutator necks are likely to give trouble if too high a peripheral speed is chosen. A peripheral speed of 15,000 feet per minute, or say 75 metres per second, seems to be thought very high for C.C. turbo-generators, but if suitable provision is made to meet the difficulties mentioned, there is no doubt that speeds up to 17,000 feet per minute are quite possible while still preserving good factors of safety. It is desirable to keep the diameter as large as possible in C.C. turbo-generators of great output, because one wishes to keep the axial length as short as possible in order to get good commutation. It must be remembered that the armature (unlike the field magnet) must have its core built of laminated iron, and the shaft must not form part of the magnetic circuit. We will generally find that after we have made the shaft of sufficient diameter to give it the right stiffness, having regard to the great span between bearings necessitated by the long commutator, and after we have provided space for a suitable spider to bring in a sufficient quantity of ventilating air (see page 206) there is none too much room left for the iron core, so that every

quarter of an inch that we can gain radially is going to help us considerably in shortening the machine.

In the case of the machine under consideration it will be found that a speed of 2750 R.P.M. is not too high for a diameter of 24 inches, or, as we are working in centimetres, say, 61 cms.

Choice of the number of poles. The number of poles to be chosen depends as with slow-speed machines upon the current to be collected, but as the speed is high, and it is not desirable to unduly increase the frequency, one does not choose any more poles than are necessary, having regard to the current per brush arm. Thus, on a slow-speed machine one would often choose 400 amperes per brush arm in preference to 500, while in a high-speed turbo-generator one is often compelled to choose 1000 amperes per brush arm rather than increase the frequency from 80 cycles to 120 cycles. The distance between the brush arms on the commutator is also an important consideration. As the diameter of the commutator is necessarily restricted, we cannot unduly increase the brush arms without getting them very near together and increasing the danger of a flash-over.

In the case under consideration we have at full load 1670 amperes, so that if we make a four-pole machine we will have 835 amperes per brush arm. As we know that many successful machines have been built with over 1000 amperes per brush arm, we accept four poles as satisfactory in this respect. If the diameter of the commutator is such that the speed is 50 metres per second (a speed by no means too high), the pitch distance of the brush arms may be as much as 27 cms. This, though not as much as we would like, is more than exists on many traction generators which are working well. If we were to choose six poles, the pitch of the brush arms would be reduced to 18 cms., rather a short distance for a traction generator, though quite permissible on a good commutating generator designed to work on a steady load. Having settled these preliminaries, we can take a calculation sheet and proceed. The machine is illustrated in Figs. 435 to 439.

The pole arc. This is settled by considering the distance that we would like to preserve between poles. With a diameter of 61 cms., giving a field bore of say 63 cms., the pole pitch may be taken roughly at 48 cms. Allowing 5 cms. for the width of the commutating pole, and another 5 cms. for space between iron, we arrive at the dimension shown in Fig. 436, which gives us a pole arc of 32·5 cms. It is a good plan to bevel off somewhat the edge of the pole as shown. This makes the coefficient $K_f = 0{\cdot}68$. As we are dealing with a continuous-current machine with a full-pitch winding, we also have $K_e = 0{\cdot}68$ (see page 13).

$$610 = 0{\cdot}68 \times 46 \times 36 \times A_g \mathrm{B}.$$

The number of commutator segments. There is no feature in the design of a traction generator more important than the provision of enough commutator segments per pole. When slow-speed engines are used to drive traction generators, it is found that in order to build a machine which will withstand bad short circuits on the trolley wires without flashing over, it is necessary to have a large number of commutator bars per pole. It is good practice to have as many as 48 bars, or even more, per pole. With a design of this kind, the advantage is that even if the load for an instant is so high that good commutation is impossible, the voltage per bar

Date 2nd May 1913. Type Turbo C.C. GEN. ~~SYN- MOTOR ROTARY~~ 4 Poles .... Elec. Spec. 13
K.W 1000; P F ......; Phase .., Volts 550-600; Amps per ter. 1670; Cycles 92; R.P.M. 2750; Rotor Amps ....
~~H P~~ .... Amps p. cond. 420. Amps p. br. arm. 840 Temp. rise 45° C. Regulation 5% comp^dg Overload 25% for 1 hour

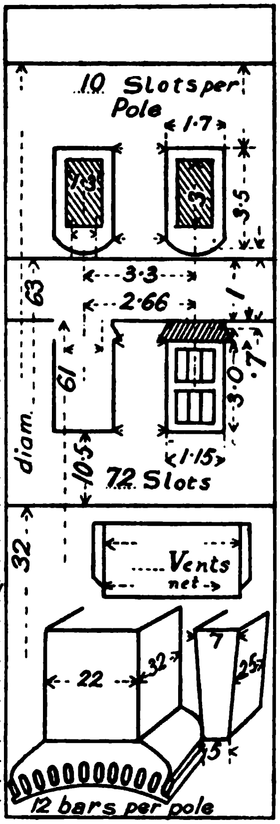
Customer ....; Order No.........; Quot. No.............; Perf. Spec. ....; Fly-wheel effect ....

Frame 61/53 Circum 192; Gap Area 1040; poss. $A_g B$ ....; poss. $I_a Z_a$ ....; $I_a Z_a$; $\frac{D^2 L \times RPM}{K.V.A.}$ $5{\cdot}5 \times 10^5$
Air 3 cu.m.p.sec. $A_g B$ $0{\cdot}542 \times 10^8$; $I_a Z_a$ 60.500 ......; Circum. 316 .....;

K_e 0.68, ... Volts = ·68 × ·46 × ·36 × ·542 ; Arm. A.T. p. pole 7600 .... Max. Fld. A.T. ....

Armature. Rev. ~~Stat.~~

Core	Dia Outs.	61	
	Dia Ins.	32	
	Gross Length	53·5	
	Air Vents 9	·8	
	Opening Min 380 Mean	1000	
	Air Velocity	88 m.p.sec.	
	Net Length 46·3 × ·89	41	
	Depth b Slots	10·5	
	Section 430 Vol	58,000	
	Flux Density	10,700	
	Loss 0·12 p. cu. cm. Total	7000	
	Buried Cu. 2600 Total	9300	11,900
	Gap Area 9000 Wts	9300	
	Vent Area 30,000 Wts	4300	
	Outs. Area 12,000 Wts	1800	15,400
Teeth	No of Segs 1 Mn. Circ.	174	
	No of Slots 72 ×1·15=	83	
	K_s	·91	
	Section Teeth	3700	
	Volume Teeth	13,300	
	Flux Density	14,600	
	Loss ·17 p. cu. cm. Total	2300	
	Weight of Iron	560	Kilogrs
Conductors	Star or Mesh Throw	1-19	
	Cond. p Slot	2	
	Total Conds 36 in series	144	
	Size of Cond. 3(·3 × 1·25)	0·98	stranded
	Amp. p. sq.	430	
	Length in Slots 53·5		
	Length outside 58·5 Sum	112	
	Total Length 1·12 m.	162 m	
	Wt. of 1,000 870 Total	141	Kilogrs.
	Res. p. 1,000 ·175 Total	·028/46	·00178
	Watts p. m	62	
	Surface p	800	
	Watts p. Sq. ·078 × ·125 = 0012	·078	
		8° C.	



Field Stat ~~or Rotor~~

Dia. Bore	63		
½ Total Air Gap	1		
Gap Co-eff. K_c	1·11		
Pole Pitch 48 Pole Arc	32·5		
K_f	·68		
Flux per Pole $9{\cdot}2 \times 10^6$			
Leakage n.l. 1·0 f.l. $1{\cdot}11 \times 10^6$	$10{\cdot}3 \times 10^6$		
Area 680 Flux density	15,100		
Unbalanced Pull			
No. of Seg. 4 Mn. Circ.	204		
No. of Slots 25 60 ×1·7=	102		
Vents none 53·5	102		
K_s Section	5500		
Weight of Iron in poles	920 Kilogrs.		
	Shunt.	**Series.**	**Comm.**
A.T. p Pole n. Load	5460		
A.T. p. Pole f. Load	5800	1670 770	6400
Surface	21,800		
Surface p. Watt	16		
I^2 R.	1370	520	1350
I R.	450	0·31	0·8
Amps.	310	900	1670
No. of Turns	1870	1	3
Mean l. Turn	1·24	1·5	1·3 m
Total Length	9300 m.	6 m.	16 m.
Resistance	122 cold 142 hot	·00038	·00048
Res. per 1,000	13·1	·057	·026
Size of Cond.	·013 sq. cm	3 sq. cm	6·5
~~Conds. per Slot~~		Compens. Wdg.	
~~Total~~		size 3·6 sq. cm	
~~Length~~		length 53 m.	
Wt. per 1,000	11·6 Kg	Resistance ·0007	
Total Wt.	108 Kg	$I^2 R$ 2240	
Watts per Sq.		Turns per pole 3	
Star or Mesh			
Paths in parallel			2

Magnetization Curve.

	Section	Length	550 Volts. B.	A.T. p. cm	A.T.	610 Volts. B.	A.T. p. cm	A.T.	650 Volts. B.	A.T. p. cm	A.T.
Core											
Stator Teeth											
Rotor Teeth	3700	3·7	13,100	12	45	14,600	21	77	15,600	35	120
Gap	10400	1	4740		4150	5250		4620	5,600		4900
Pole Body	680	17	13,500	15	260	15,000	26	440	16,000	34	580
Yoke	860	37	10,800	9	330	12,000	12	450	13,800	20	740
					4785			5587			6340

Commutator.

Dia. 40 Speed 48 m/sec
Bars 144
Volts p. Bar 17
Brs. p. Arm 16
Size of Brs. 2·5 × 5
Amps p. sq. cm 4·2
Brush Loss 3500+4000
Watts p. Sq. cm 0·4

EFFICIENCY	1¼ load.	Full.	¾	½	¼
Friction and W	40	40	40	40	40
Iron Loss	9·3	9·3	9·3	9·3	9·3
Field Loss shunt	1·4	1·4	1·4	1·4	1·4
Arm & c I^2R	14·5	9·9	5·5	2·4	·6
Brush Loss	5·0	4·0	3·0	2·0	1·0
	70·2	64·6	59·2	55·1	52·3
Output	1250	1000	750	500	250
Input	1320	1065	809	555	302
Efficiency %	94·7	94	92·7	90	83

Mag. Cur Loss Cur.
Perm. Stat. Slot
,, Rot. Slot × =
,, Zig-zag
2 × × =
1·77 × =
End × × =
Amps. Tot.
τ = ; X_s =
S_1/S_2 ; r_s = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

is too low to maintain an arc between successive bars, so that a flash on the commutator occurring at the instant of the short circuit is not readily carried around from brush to brush. There is a certain critical voltage per bar at which the liability to flash is very much increased. This critical voltage depends somewhat on the arrangement of the field system. For an ordinary uncompensated field system an average voltage of 20 volts per bar is near the danger limit, while for a perfectly compensated machine the critical voltage per bar may be taken as somewhat higher. Still, it is good practice even on a compensated machine to keep the volts per bar well below 20 if it can be done.

If we have 36 bars per pole on the commutator, it will give us 17 volts per bar. A higher number of bars would give a better performance, but if we take more than 18 coils per pole it will be difficult to find room for them on an armature 61 cms. in diameter. As mentioned on page 517, the method which we propose to adopt for obtaining 36 bars per pole when we have only 18 coils per pole is that in which connectors are brought from the back of the armature to alternate bars. The method of supporting these connectors is illustrated in Fig. 439. Thus, while we have 36 bars per pole, we have only 36 conductors in series, so that the armature ampere-turns are exactly half what they would be if we had 36 complete coils per pole.

At this point it is well to check the ampere-wires per centimetre. The current per conductor will be 420 amperes, which multiplied by 144 gives us 60,500 ampere-wires, and 316 ampere-wires per cm. This figure is not too high; but by reason of the small depth of slot necessary, if we are to maintain good commutation at high frequency, it is quite high enough.

Size of conductors. It will be found that conductors lying in a slot with only comparatively thin insulation between copper and iron are so well cooled that they can be worked at a fairly high-current density—in this case as high as 430 amperes per sq. cm.; whereas in the end connectors, which must be huddled together and subjected to very much poorer cooling conditions, the current density ought to be low. It is quite worth while, in a case of this kind, to use a different copper section for the end connectors, and also a different shape. Electric welding has now been carried to such a state of perfection that it is a comparatively simple matter to connect two conductors of different sections by welding. In this case the involute shape has been chosen for the end connectors, for the following reasons: If the barrel form of end connector were chosen, the end bell required to support the connectors would have to project a long way above the surface of the armature; and as it is of such a large diameter, the stresses in the end bell due to its own weight would be exceedingly great. With the involute end connector, supported in the manner shown in Fig. 438, the supporting rings can be made of great section without projecting unduly beyond the periphery of the armature, so that the self-stress is much reduced, and the axial length of the machine is not greater than it would be for a barrel winding. Another advantage is, that it is comparatively a simple matter to provide for a wide ventilating duct between the two halves of the end connector. An end view of the involute connectors can be seen from Fig. 439. The finger-plates of the armature are made of silicon bronze of great tensile strength and high conductivity. The two outer end plates are made of the same metal.

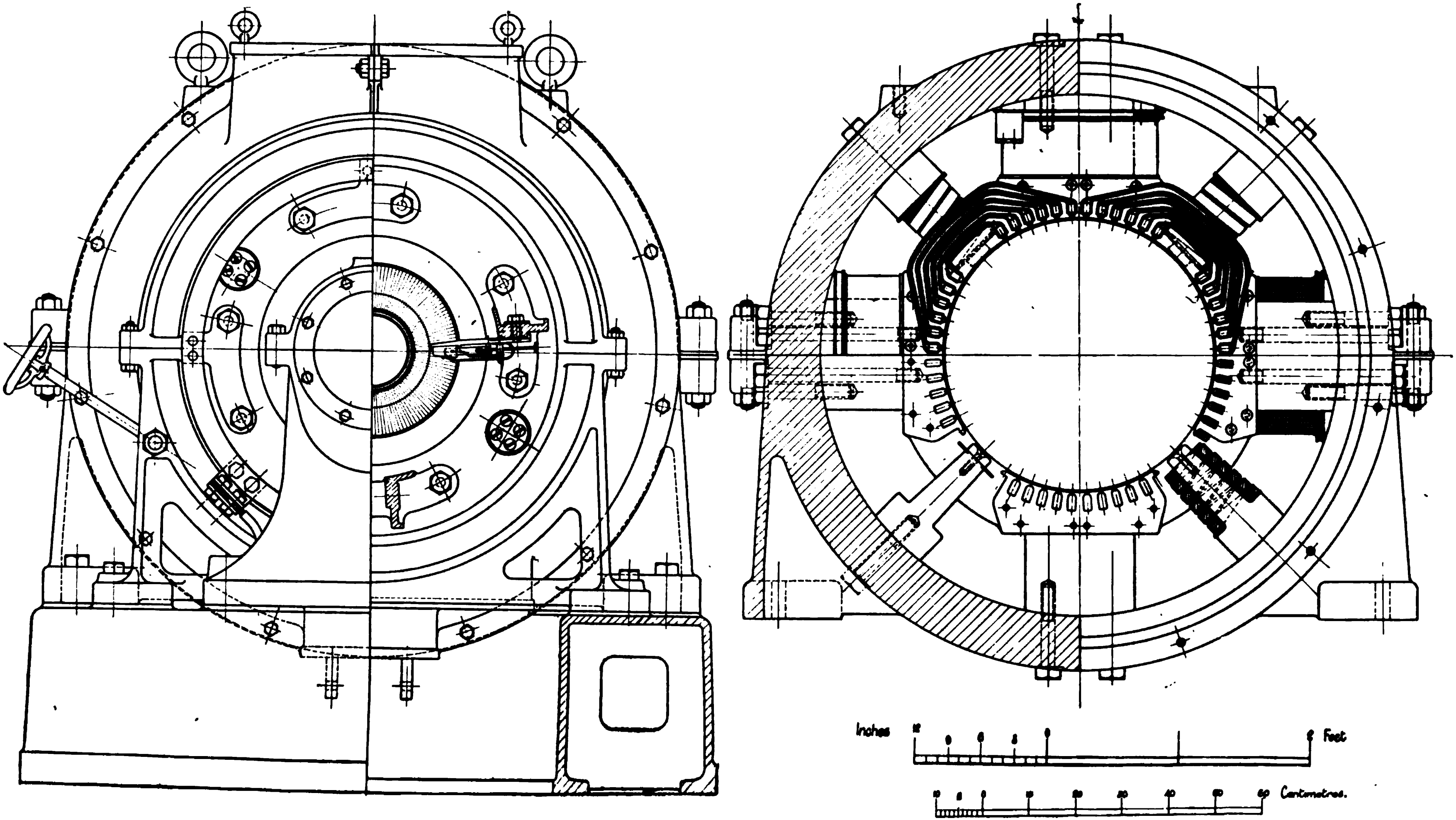
In between the inner and outer end plates are bridge pieces made of phosphor bronze castings, which are supported by double spigots on the end plates. The lower tier of armature conductors, with their end connectors, which are of stranded copper, are assembled around the armature on a large diameter, with the involute end connectors properly interleaved. The diameter at which they are originally assembled is sufficiently great to get over the difficulty of interleaving the connectors. The diameter is then reduced in stages until the lower tier lies in the bottom of the slots. The outer tier is then assembled in the same manner and inserted in the slots; thimble connectors are then used to bridge between the inner and outer connectors, as shown in Fig. 438. These connectors are suitably taped over, but allow sufficient room for air to enter the ventilating duct between the tiers. The ventilating air gets between the bridge pieces where these flank the conductors; and at the point where the bridge piece supports the top conductor, two ducts are milled out to enable the air to escape.

The method of supporting the connectors from the alternate bars to the back of the armature is also seen in Fig. 439. These are grouped in batches, each batch surrounded by a metal sheath, which is threaded through a slot in the arm of the spider. It will be seen that there are 14 connectors in each sheath, although only twelve are required to go to the commutator bars. The outer connectors of each batch are short-circuited together so as to form a closed conductor embracing the air space between the arms of the spider. The object of this short-circuited conductor is to reduce the self-induction of the connectors lying nearest to the air space. It will be seen that in operation, while one connector is carrying an increasing current, the connector next to it must be carrying a decreasing current; so that the self-induction of the connector can only be due to a flux which lies between itself and the adjacent conductor. It will be good practice to make the straight part of the conductors lying in the slots also of stranded copper; but if solid conductors are used, it is best to make the conductor near the mouth of the slot shallower than the conductor at the bottom of the slot. As the frequency is 92 cycles, a solid conductor near the mouth of the slot would be subjected to serious eddy-currents unless its depth were reduced to about 1·1 cms. The conductor at the bottom of the slot can, however, be made 1·4 cms. without much fear of eddy-currents (see page 144); so that the average height of a conductor would be 1·25 cms. If the width is 0·9 cm., the whole will go in a slot $1{\cdot}15 \times 3$ cms. The wedge at the top of the slot is by preference made to the shape shown in the drawing, the depth of the wedge being 0·7 cm. It will be seen from the calculation sheet, page 531, that the watts per sq. cm. amount to 0·078, which with a thickness of insulation equal to 0·125 cm. will give a difference of temperature between copper and iron of 8° C.

Magnetic loading. A rough calculation, or our previous experience, leads us to allow about 10 volts drop in the armature at full load; so we may take the generated voltage to be 610. Thus we get the equation:

$$610 = 0{\cdot}68 \times 46 \times 36 \times A_g\mathrm{B},$$
$$A_g\mathrm{B} = 0{\cdot}542 \times 10^8.$$

Saturation in the teeth. The mean circle through the teeth is 174 cms. The total width of all the slots is $72 \times 1{\cdot}15 = 83$; so that the total width of all the teeth



FIGS. 435 and 436.- End view and section of 1000 K.W. C.C. turbo generator, 550 to 600 volts, 2750 R.P.M., to comply with Specification No. 13.

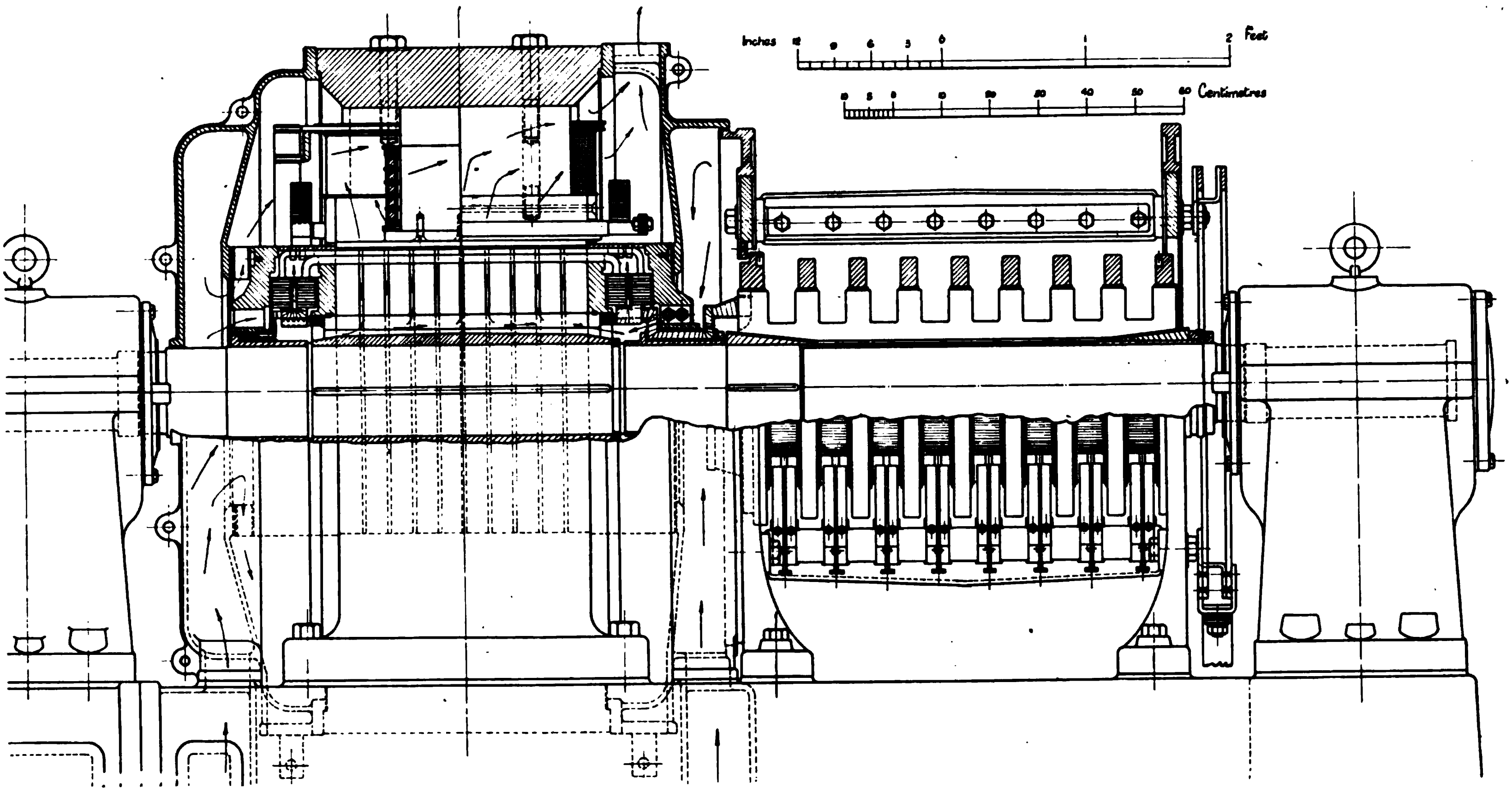


FIG. 437.—Longitudinal section of 1000 K.W. C.C. turbo-generator, showing construction of radial commutator.

is 91 cms. Now, at this high frequency, it is not desirable to work the teeth at a high density; so that a length of iron has been chosen to keep the density as low as 14,600 lines per sq. cm. The gross length is 53·5 cms., and the nett length 41 cms. 91×41 gives 3700 sq. cms. section of the teeth. The volume of the teeth is 13,300 cu. cms.; and as the loss is 0·17 watt per cu. cm., the total loss in the teeth is 2300 watts. The flux-density behind the slots is 10,700, giving a loss of 0·12 watt per cu. cm., and a total iron loss of 9300.

The investigation of the cooling conditions will be easily followed from the calculation sheet taken in conjunction with the description given on page 324. The arrangement of the compensating winding can be easily followed from Fig. 436 and the calculation sheet.

Commutating windings. In working out the strength of the commutating pole, we must refer to the drawings. Adopting the formulae given on page 480:

$$L_n = 1{\cdot}25\left(\frac{3}{3 \times 1{\cdot}15} + \frac{0{\cdot}7}{1{\cdot}15}\right) = 1{\cdot}85,$$

$$L_c = 1{\cdot}25\left(\frac{2{\cdot}5}{2 \times 1}\right) = 1{\cdot}56,$$

$$L_s = 0{\cdot}46 \times \frac{58{\cdot}5}{53{\cdot}5}\left(\log_{10}\frac{58{\cdot}5}{2{\cdot}5}\right) = 0{\cdot}69,$$

$$L_n + L_c + L_s = 4{\cdot}1,$$

$$\mathrm{B}_c = 2{\cdot}8 \times 4{\cdot}1 \times \frac{840}{5{\cdot}83} = 1650.$$

The axial length of the commutating pole will be about 32 cms.; we must increase B_c in the ratio $\frac{53{\cdot}5}{32}$ to give us 2760. The effective ampere-turns per pole should be

$$1 \times 2760 \times 1{\cdot}08 \times 0{\cdot}796 = 2370.$$

The armature ampere-turns per pole are 7600; so that the total ampere-turns on the commutating pole, including those on the compensating winding, should be 10,000. We can obtain these ampere-turns by three turns on the compensating winding and three turns on the commutating pole. It will be found convenient to divide the compensating winding into two paths in parallel, each carrying half the current, so that each bar has only to carry half the current. Thus we get twelve bars per pole, as shown in the drawing. The method of working out the saturation curve, the cooling conditions on the field winding, and the efficiency, will be easily followed from the calculation sheet.

Commutator. This is of the radial type provided with 8 grooves, or 16 working faces. There are 16 brushes per arm, each measuring $2{\cdot}5 \times 5$ cms., giving a total area of 400 sq. cms. per terminal, whereas the density is only 4·2 amperes per sq. cm. A burnt graphite brush of a type similar to the L.F.C. brush of the Le Carbone Company is suitable for this purpose. The total brush losses amount to 7·5 K.W. This gives us 0·4 watt per sq. cm. estimated on the surface of the commutator; but as a great part of the heat is conducted into the brushes and brush-holders, which afford a very well-ventilated cooling surface, the temperature of the commutator will not be too high. The method adopted of connecting between the

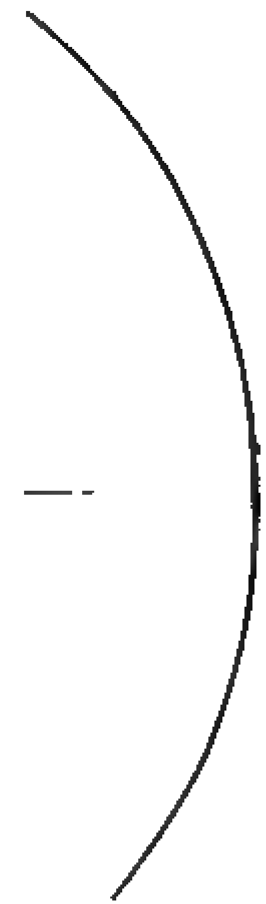


FIG 438.—Section of end winding of 1000 K.W. C.C. turbo-generator, showing methods of supporting winding and making connections to commutator bars.

FIG. 439.—Sectional drawing of armature of 1000 K.W. turbo-generator, showing ventilating plate, involute connectors, connections to back of armature, commutator necks, and special connections between winding and commutator bars.

armature conductors and the commutator bars is illustrated in Fig. 438. Special U-shaped connectors are assembled on the spider in groups, the whole being held in position by a ring which is divided into two parts and screwed together in the manner shown. This ring carries a number of projections, upon which the outer end ring of silicon bronze is supported after the armature winding has been put into place.

CHAPTER XIX.

ROTARY CONVERTERS.

In what cases they are suitable. For general purposes the rotary converter is the most efficient machine for converting from alternating to continuous current. There are some cases in which the motor-generator is more suitable than the rotary for conversion from A.C. to C.C. If the alternating voltage is very unsteady, and it is required to have a very steady continuous voltage, then the motor-generator is the better machine to use. Again, if it is desired to reduce the continuous voltage to zero and to bring it gradually to full value (as, for instance, in the Ward-Leonard control), the motor-generator is the machine generally specified, though it would be possible to design a modified rotary converter for this class of work. It used to be said that a motor-generator had the advantage of being more easily started after a general shut-down, but now that rotaries are made self-starting and self-synchronizing, the contention no longer holds.

Many of the large continuous-current railway and tramway systems of the world obtain their current through rotary converters, and a frequency of 25 cycles has been commonly used for such systems. During the last ten or twelve years the 50-cycle rotary has become established as a perfectly reliable machine both for traction and general lighting and power supply. There is no doubt that where a new system is being put down, mainly for continuous-current traction, 25 cycles or 33 cycles will be chosen in preference to 50 cycles. But where a system of 50 cycles is already in use, there is no difficulty in supplying continuous current for tramway purposes by means of rotary converters suitably designed to meet severe conditions sometimes occurring in traction service.

The best frequency. In general it may be said that for fairly high-voltage continuous-current work (750 to 1500 volts) low frequency is to be preferred, because it allows us to design the rotary with a greater distance between brush arms than would be possible with a high frequency. For low-voltage work, as, for instance, in the supply of continuous current at 250 volts or lower for electrolytic purposes, the higher frequency is suitable, because it gives a cheaper machine with a large number of brush arms, and the current per brush arm is not so high as it would be on a low-frequency machine.

The pitch of the brushes. The distance between the brush arms and the frequency are related on account of the following consideration: During one complete

cycle, a point on the commutator must travel through a distance equal to twice the distance between two consecutive brush arms. Thus, in a 50-cycle converter, if the pitch of the brushes is 10 ins., then the speed of the commutator must be 20 ins. in one-fiftieth of a second, or 1000 ins. (about 25 metres) per second; that is, 5000 feet per minute. As it is thought that this is about as high a speed as one should run an ordinary commutator, 10 ins. is about the maximum pitch one will find on 50-cycle converters. For the same commutator speed the brush arms could have a 20-inch pitch on a 25-cycle rotary, though for reasons of economy the pitch is more commonly about 13 ins. It is quite likely that, as the design of brush holders and the construction of commutators is improved, the commutator speed will be increased and the distance between brush arms may be increased where a greater distance is desired.

Variation of the voltage. The differences in the characteristics of various installations of rotary converters lies mainly in the range of voltage over which the machines are designed to work and the different methods * employed to effect the change in the voltage at which the continuous current is supplied.

Putting out of account for the moment the split-pole converter and other machines of a similar nature, we may say that the ratio between the voltage on the slip-rings and the voltage on the commutator remains nearly constant † independently of the excitation.

Table XXII. gives the values of the ratios between the voltage on the slip-rings and the voltage on the brushes on the commutator for two-phase, three-phase and six-phase machines as ordinarily built. These ratios ordinarily have not in practice the values they would have if the field-form of the machine were sinusoidal. The widening of the pole (as commonly done on rotaries to increase the output) has the effect of making the ratio of the A.C. voltage to the C.C. voltage somewhat smaller than it would be with a sine-wave field-form.

TABLE XXII.

RATIOS OF A.C. TO C.C. VOLTAGE ON ROTARY CONVERTERS AS AFFECTED BY THE RATIO OF POLE ARC TO POLE PITCH, ALLOWING FOR NORMAL BEVELLING OF POLES.

$\frac{\text{Pole arc}}{\text{Pole pitch}}$	0·8	0·75	0·7	0·65	0·6
Single-phase	0·68	0·695	0·71	0·72	0·74
Three-phase	0·6	0·61	0·62	0·635	0·65
Four-phase	0·48	0·49	0·5	0·52	0·53
Six-phase rings 1 and 4	0·68	0·695	0·71	0·72	0·74
„ rings 1 and 3	0·6	0·61	0·62	0·635	0·65
„ rings 1 and 2	0·34	0·35	0·355	0·365	0·375

* See page 546.

† We say "nearly constant," because it is possible by greatly over-exciting or under-exciting a three-phase rotary, to change the voltage on the commutator some 3 per cent., while the voltage on the slip-rings remains constant. On a three-phase machine there is at certain instants a considerable angle between the connection to a slip-ring and the connection to the commutator bars upon which the brushes momentarily touch. The leading or lagging current passing through this part of the winding will give a positive or negative boosting effect, but this effect is not sufficient to give a wide range of voltage.

TABLE XXIII.

RATIOS OF A.C. AMPERES PER SLIP-RING TO C.C. AMPERES PER TERMINAL, ASSUMING AN EFFICIENCY OF 95 PER CENT.

Pole arc / Pole pitch	0·8	0·75	0·7	0·65	0·6
Single-phase	1·54	1·51	1·47	1·44	1·4
Three-phase	1·03	1·01	0·98	0·94	0·93
Four-phase	0·77	0·74	0·73	0·72	0·69
Six-phase	0·515	0·5	0·49	0·48	0·46

Table No. XXIII. gives the values of the ratios of A.C. amperes per slip-ring to C.C. amperes per terminal, assuming an efficiency of 95 per cent.

These values are given on the assumption that brushes are placed on the neutral point, the excitation normal, and that the field-form is such as one finds on ordinary commercial machines.

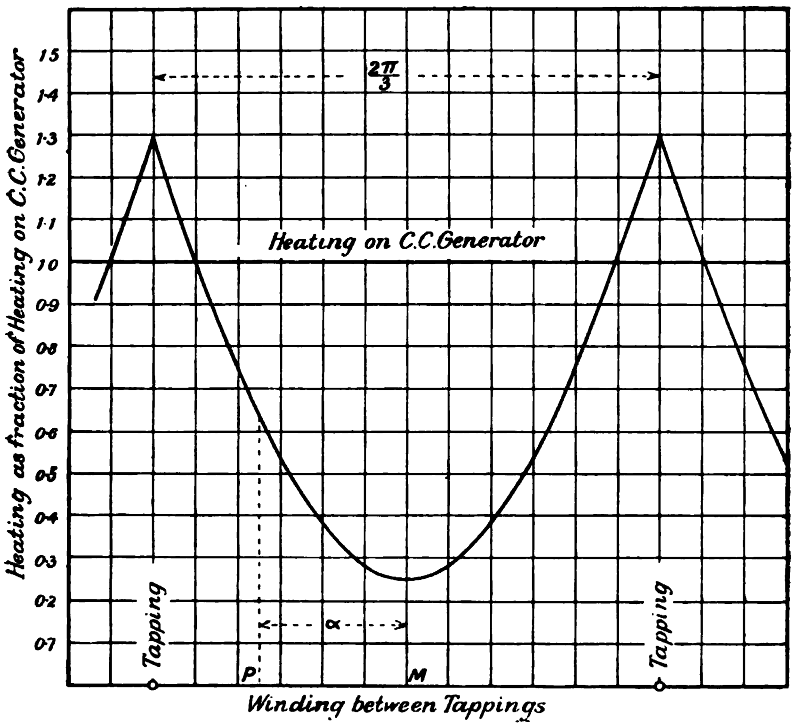


FIG. 500.—Showing the heating of the various parts of the winding of a 3-phase rotary converter running on unity power factor compared with the heating of the conductors of a C.C. armature of the same output. $h=1·04$; $k=0$.

The heating of the armature conductors. The theory of the heating of the armature conductors of a rotary converter is so fully dealt with in standard

text-books * that there is no necessity to give it here. The heating is greater in those conductors lying near the points from which tappings are taken than in the conductors lying midway between tappings.

Figs. 500 and 501 show the rate of production of heat in the various parts of the armature winding of a 3-phase rotary converter as compared with the rate of production of heat in the same winding used as a C.C. generator. The curves in this

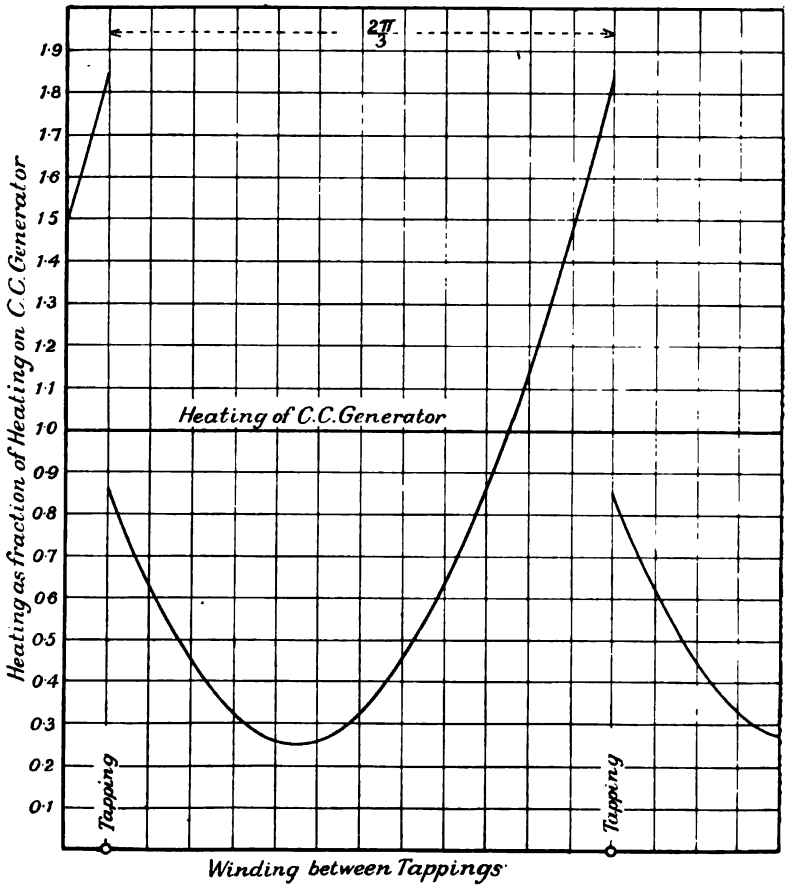


FIG. 501.—Showing the heating of the various parts of the winding of a 3-phase rotary converter with current leading by 15° as compared with the heating of a C.C. generator carrying the same load. $h=1{\cdot}04$; $k=0{\cdot}26$.

figure and in the two following figures have been plotted on the assumption that the power supplied on the A.C. side of the converter is 4 per cent. greater than the power given out on the C.C. side. At unity power factor (Fig. 500) the heating is least at the point in the winding midway between the tappings, and it is here only 0·25 of the heating on a C.C. generator. At points close to the tappings, however, the heating is greater than on a C.C. generator.

* Barr and Archibald, *The Design of Alternating-Current Machinery* (Whittaker), 1913. Woodbridge, *Proc. Amer. I.E.E.*, vol. xxvii. p. 204.

If m is the number of slip-rings and α is the angular distance of any point P from the mid-point M of a section of the winding, and ϕ is the angle of lag of the current, the heating of the winding at the point P, expressed as a fraction of the C.C. heating, is

$$1+\frac{8H_x^2}{m^2\sin^2\frac{\pi}{m}}-\frac{16H_x\cos(\alpha+\phi)}{\pi m\sin\frac{\pi}{m}} \quad \text{.......................(1)}$$

In this formula $H_x=\sqrt{h^2+k^2}$, where h is the ratio of A.C. power to C.C. power and k is the ratio of the wattless current to the power current at unity efficiency. Thus, where 4 per cent. losses are supplied by the A.C. current, $h=1{\cdot}04$, and where the wattless current is 0·26 of the working current ($\phi=15°$), then $k=0{\cdot}26$.

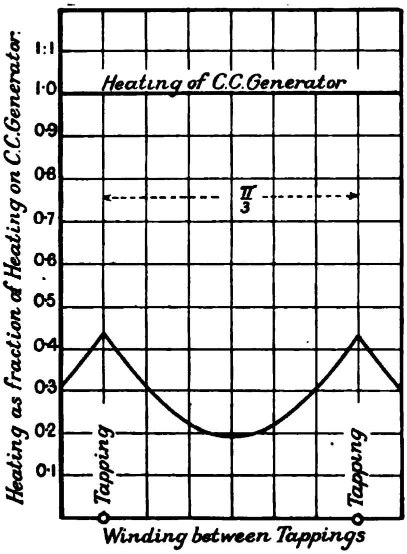


FIG. 502.—Showing the heating of the various parts of the winding of a 6-phase converter running at unity power factor as compared with the heating of a C.C. generator carrying the same load. $h=1{\cdot}04$; $k=0$.

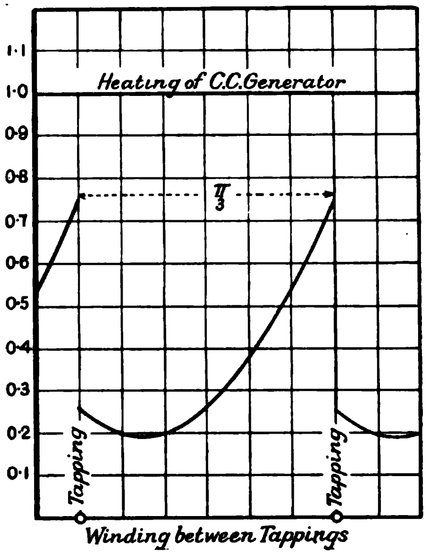


FIG. 503.—Showing the heating of the various parts of the winding of a 6-phase converter with the current leading by 15° as compared with the heating of a C.C. generator carrying the same load. $h=1{\cdot}04$; $k=0{\cdot}26$.

Fig. 501 shows the effect of making the current lead by 15° in a three-phase winding. The winding is supposed to be moving from right to left. In front of each tapping the winding tends to get hot, the heating effect in some parts of the copper being 1·8 times as great as in a C.C. machine carrying the same load. When the current lags, it is the winding immediately following the tapping that gets hot.

In a six-phase machine the heating is much more evenly distributed over the winding. Fig. 502 shows the heating at unity power factor, and Fig. 503 the heating where the current leads by 15°.

It will be seen that at unity power factor, the loss in the copper near the tapping is 0·43 of the C.C. loss, while at power factor 0·966 the loss is 0·74 of the C.C. loss.

The average loss in the whole winding is dependent upon the power factor. The ratio of the loss in the converter to the loss in the C.C. generator at the same load is

$$1+\frac{8H_x^2}{m^2\sin^2\frac{\pi}{m}}-\frac{16h}{\pi^2}. \qquad (2)$$

For a six-phase machine $m=6$, and if $h=1$ the above expression simplifies down to

$$0{\cdot}268+0{\cdot}89k^2. \qquad (3)$$

The second term of this expression gives us the loss due to the wattless load. As a matter of fact, owing to eddy currents, the loss is greater than given by this expression (see page 144).

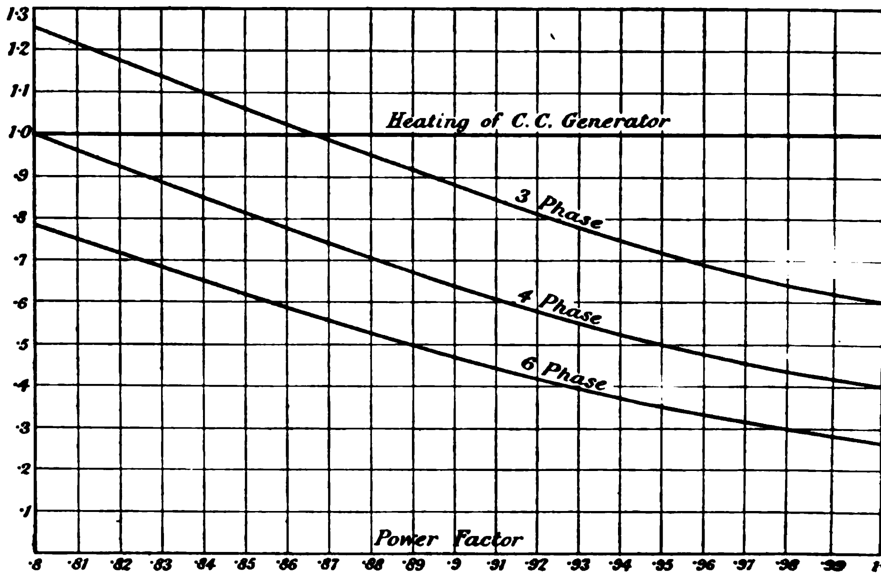


FIG. 504.—Average loss in the armature conductors of a rotary converter as compared with the loss in the same winding used as a C.C. generator.

If we neglect the eddy-current losses and take $h=1$, the heating coefficients for 3-phase, 4-phase and 6-phase rotary converters vary with the power factor in the manner shown in Fig. 504. These curves serve to calculate the efficiency where the efficiency is arrived at by the measurement of separate losses, but the actual loss will be somewhat heavier (see page 545).

Now the question arises, how much extra copper are we to put in the armature when the power factor is less than unity, say 0·966 ? Consider the six-phase case. We are not merely concerned with the raising of the average loss from 0·27 to 0·33 in the case given above. Nor are we to say that the temperature will be raised in proportion to the losses in the conductor adjacent to the tapping (from 0·43 to 0·74 in the case given), because the heat is rapidly conducted away from a single hot conductor contiguous with others. We are really most concerned with the rise in

temperature of the group of conductors lying near the tapping, and we may take the following rule as giving results which are accurate enough for practical purposes.

For a six-phase machine, take the working current (at unity power factor) for any load as if it caused a loss equal to 0·3 of the loss there would be in a C.C. generator with the same load. Take the wattless current as if it were entirely independent, and calculate the loss just as we would for a C.C. generator with the same current. This is the same as taking the coefficient of the second term in (3), page 544, as equal to unity. Now add the two losses, and the sum will be nearly proportional to the rise in temperature of the hottest part.

EXAMPLE 59. On a 6-phase converter. Power factor 0·966, leading current 0·28 of working current. Take working current loss as 0·3 and wattless current loss as $(0{\cdot}28)^2 = 0{\cdot}078$. Loss = 0·378, the loss there would be in a C.C. dynamo carrying the same current. Note: this method does not apply when calculating the total loss in the armature for the purpose of getting the efficiency. For this latter purpose the figure would be 0·33 as given above, not 0·378.

EXAMPLE 60. In a case of a 1000 K.W. converter we are required to supply 300 wattless K.V.A. on the H.T. side of the transformer when running at full load. Before we can begin to get leading current on the H.T. side of the transformer, we must supply its magnetizing current from the converter and also have the current leading enough on the low-tension side to neutralize the tendency of the H.T. current to lag, owing to the self-induction of the transformer.

The transformer in this case will be built for small reactance, so an allowance of a further 100 leading wattless K.V.A. will be sufficient to cover the difference in phase between the low-tension and the high-tension currents. Strictly speaking, we should get this information from the transformer designer, but in practice, in fixing these preliminary matters, one makes an allowance and proceeds. The exact effect of the transformer self-induction will be seen later (see Fig. 595). We then want 400 wattless K.V.A. on the low-tension side, that is to say, 0·4 of full-load working-current leading. This affects the design of the converter in two particulars. It not only calls for more copper in the armature; it calls for more copper in the field magnet in order that the converter may be sufficiently over-excited. For the moment we are only concerned with the armature copper. The working current is 1900 amperes, and on a 14-pole machine will be 136 amperes per conductor. Take the leading current at 0·4 of this, or 55·4 amperes. Through one ohm of resistance the working current would produce

$$136 \times 136 \times 1 \times 0{\cdot}3 = 5550 \text{ watts loss.}$$

While the wattless current would produce

$$55{\cdot}4 \times 55{\cdot}4 \times 1 = 3000 \text{ watts loss.}$$

Total 8550 watts. While on a continuous-current generator one ohm of resistance would give $136 \times 136 = 18{,}500$ watts, so the heating is $8550/18500 = 0{\cdot}46$ of what it would be in a continuous-current generator. Now the size of the copper strap required depends upon the number that are grouped together in one coil and the cooling conditions on the surface of the coil. If our eight conductors per slot are insulated with mica and paper and tape to a thickness of 0·05 inch and say 0·01 inch of air space, it is easy, from the considerations given on page 222, to show that for a difference in temperature between copper and iron of 20° C. we can pass at least 0·8 watt per sq. inch. Now our coil will have a surface of about 36 sq. ins. per foot run, so that it will pass to the iron about 29 watts per foot run. If we have a barrel winding fairly well ventilated, we will at a high speed easily get rid of more than 0·8 watt per sq. in. in the part outside the slots. In calculating the loss in the slots we must not forget the eddy-current loss in the straps on the top of the slot. For straps ½ inch deep and with the dimensions of slot given in Fig. 513, it will be found by calculation like that given on page 149 that the eddy-current loss is 0·4 of the loss calculated from current and resistance only. This gives the coefficient 1·4 used below. If now we take the efficiency at 90 per cent. and the current to be converted 142 amperes per conductor, we have the resistance of the strap per foot length at the running temperature

$$x \times 8 \times 142 \times 142 \times 0{\cdot}46 \times 1{\cdot}4 = 29 \text{ watts per foot run,}$$

$$x = 0{\cdot}00028 \text{ ohm per foot at } 65^\circ \text{ C.,}$$

$$x = 0{\cdot}00024 \text{ ohm per foot at } 25^\circ \text{ C.}$$

The section must be such as to have a resistance of 0·24 ohm per 1000 ft.

$$\text{Area of strap } \frac{0{\cdot}0082}{0{\cdot}24} = 0{\cdot}034 \text{ sq. in.}$$

This would give us an apparent current density of 4200 amperes per sq. inch. But before deciding on this size we must look at the over-load guarantees. Suppose that there is an over load of 25 per cent. for three hours. This, so far as heating is concerned, is the same as if it were a continuous over load, because a high-speed machine like this will get very near its maximum temperature in the armature copper in the course of an hour's run. It may be, however, that there is no guarantee to run on a leading power factor at 25 per cent. over load, and it may be that there is no reason why it should not be run at or near unity power factor. Now it is easy to show by the rule given above that the heating on 25 per cent. over load at unity power factor is about the same as the heating at full load with the addition of 300 wattless K.V.A. In one case we have

$$175 \times 175 \times 0{\cdot}3 = 9200,$$

and in the other

$$142 \times 142 \times 0{\cdot}46 = 9300.$$

If we are allowed 50° C. rise on 25 per cent. over load, no special provision need be made to meet this guarantee. There is sometimes the question whether or not it is worth while to put a little more copper in the armature so that the same design will do for a machine which has to run under more stringent conditions. That question is generally settled by the standard size of copper straps kept in stock and the room available in the slot for which we happen to have a die. We cannot take account of these things here.

In actual practice, of course, we do not go through this long calculation. We know from experience that we can on a six-phase converter work the copper at from 3000 amperes per sq. in. to 6000 amperes per sq. in. according to the power factor. We choose a suitable standard strap accordingly, and if in doubt we check the cooling on the basis of 0·8 watt per sq. in. for a 500 volt insulation.

Variation of voltage. The voltage can be changed (1) by rocking the brushes, (2) by changing the field-form. Either of these methods can be used to vary the voltage on the continuous-current side, while the A.C. voltage remains constant; but the more usual practice, when it is required to change the C.C. voltage, is to change the A.C. voltage supplied to the taps on the armature winding.

This can be effected by one of the following methods:

(1) By changing the excitation of the A.C. generator supplying the rotary. This can be done when the A.C. generator supplies nothing but the rotary load, and it is desired to change at the same time the voltage of all the rotaries connected to the generator; for instance, where turbo-generators and rotaries are put down for supplying continuous current, it may be for electrolytic work. Here a very wide range of voltage variation can be obtained.

(2) By means of a synchronous A.C. booster in series with the conductors supplying the rotary. A booster of this kind is usually mounted on the shaft of the rotary, and has the same number of poles, so that it is necessarily synchronous. The advantage of this method of changing the voltage is, that it gives complete control of the voltage of each rotary independently, and at the same time allows of an independent adjustment of the power factor.

The armature may be placed between the slip-rings and the taps to the rotary winding, as shown in Fig. 516, or it may be outside the slip-rings and connected to the low-tension side between the transformer and the slip-rings. When the current to be dealt with is very great, and the voltage of a number of rotaries must be varied at the same time, it may be convenient to connect an external booster in series with the high-tension side of the transformer. In this case very great care should

be taken with the insulation of the booster, or the factor of safety of the whole plant will be lowered. Sometimes it may be convenient to drive the A.C. booster by means of a synchronous motor, but this plan is not so efficient or so desirable as connecting it to the rotary itself. The design of an A.C. booster is worked out on page 582. The right way of making the connections between the armature windings of the booster and the armature windings of the rotary are given in Fig. 505. A suitable diagram of connections for a rotary and booster is given in Fig. 506.

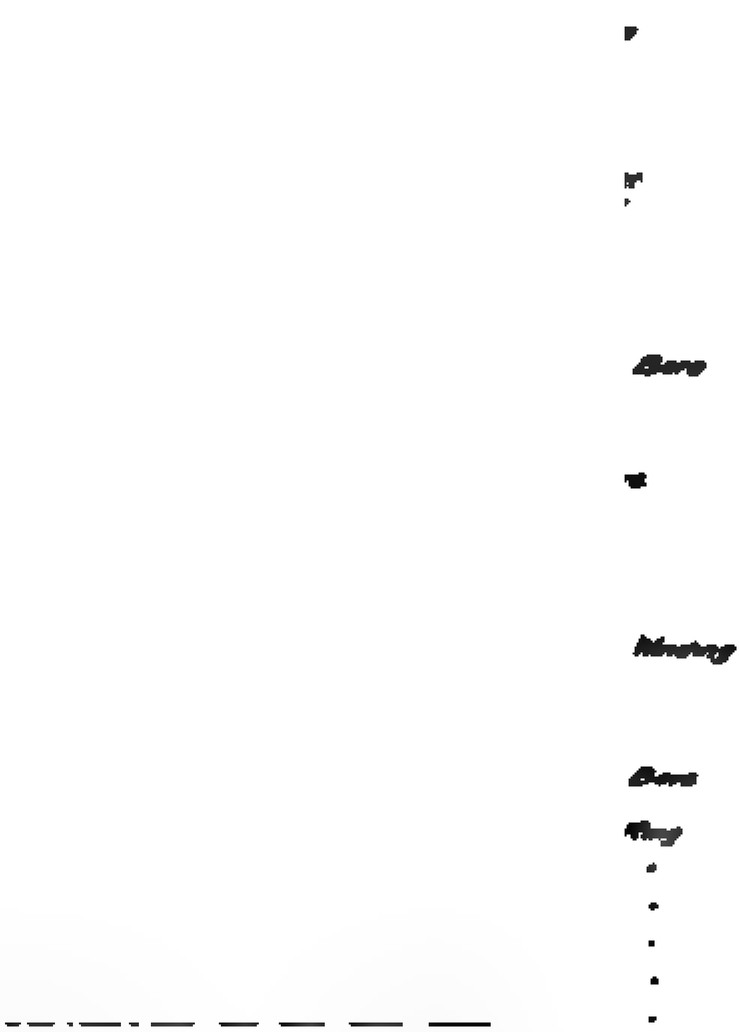


FIG. 505.—Showing armature winding of 6-phase rotary converter and armature winding of A.C. booster. The diagram shows the correct relation between the booster windings which are in star and the converter windings which are in mesh, and also the position of the poles.

(3) A third way of changing the A.C. voltage supplied to a rotary is by means of an inductance in the circuit between the rotary and the supply mains. If a lagging current is drawn through this inductance, it gives a drop in voltage at the slip-rings. If a leading current is drawn, it raises the voltage. The lagging or leading current is obtained by under-exciting or over-exciting the rotary. The self-induction may be obtained by suitably designing the transformer feeding the rotary, or special reactance coils may be added in series with either the high-tension mains or the low-tension mains. The most usual method is to design the transformer so as to have considerable self-induction, because the losses are very little if at all increased by so doing, whereas reactance coils would add considerably to the I^2R and iron

losses. Where the reactance required is not too great (10 or 12 per cent.), it can be obtained by simply grouping the high-tension coils of the transformer together and the low-tension coils together, so as to cause magnetic leakage between them. This somewhat cheapens the transformer and increases its factor of safety. Where

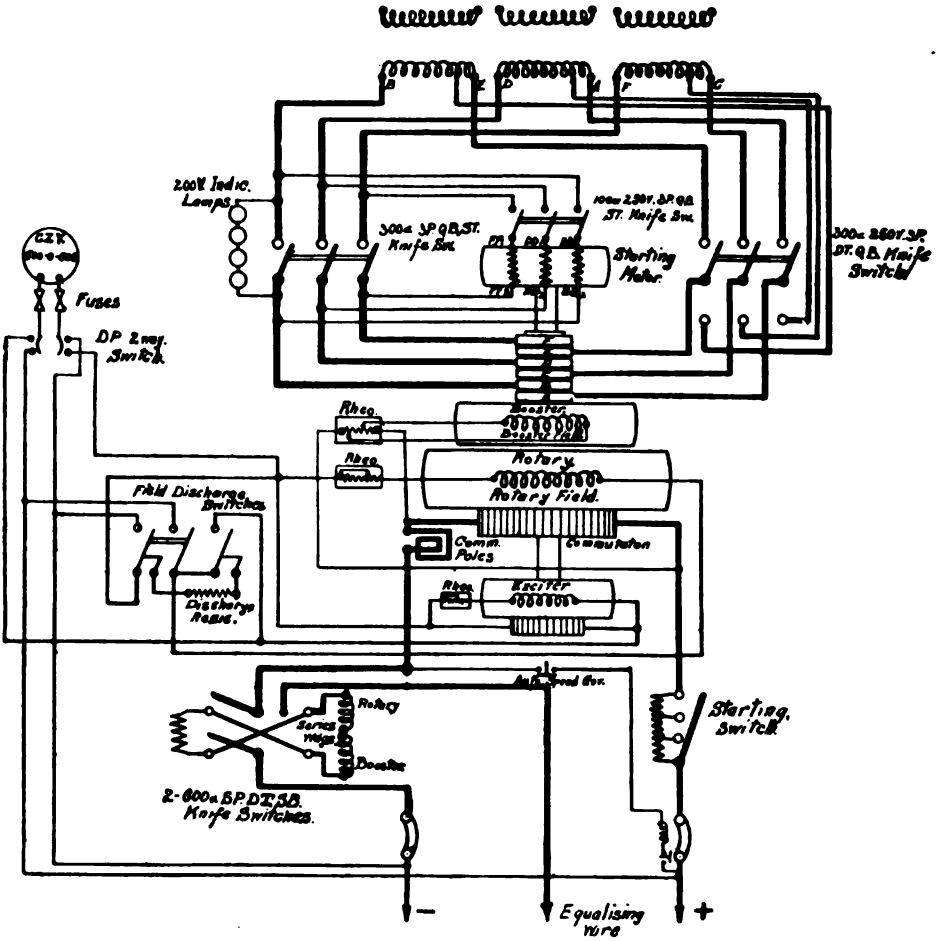


FIG. 506.—Diagram of connections of rotary converter and booster to auxiliary switches.

a very high reactance is required, it may be necessary to add iron to the leakage paths in the transformer. One advantage of external reactance coils is that they can be readily cut in and out of service as required.

The considerations which determine the amount of reactance to have in circuit are reviewed on page 599, where the theory of the added reactance is worked out.

(4) A fourth way of varying the voltage is by means of an induction regulator. This in effect is the same as an A.C. booster, but an induction regulator is more expensive than a rotating booster, and is not self-ventilating. It has, however,

the advantage that it does not interfere with the commutation, and may, therefore, be used for a very wide range of voltage.

(5) A fifth way is by means of taps on the transformer. These taps may be either on the high-tension side of the transformer or the low-tension side. In the case of large transformers, they are usually put on the high-tension side, because on the low-tension side the cost of bringing out taps is much greater, and the voltage per turn is usually too high a percentage of the whole voltage.

The main difficulty with this method is in the changing of the taps when on load. Unless some device of a more or less complicated nature is added, we have a sudden jump in the voltage as we pass from one tap to the next. Moreover, it is necessary to connect to one tap before disconnecting from the last, and this causes a short circuit on part of the transformer windings lying between the taps, unless a " preventative " resistance or some other equivalent device is employed. Modern designs of the controllers for connecting two successive taps, and for the prevention of sparking, have made this method much more acceptable than it has been in the past. Where a really satisfactory means of changing the taps can be employed, this method of changing the voltage can be recommended. It is more efficient than any other method, and for large units and wide ranges its first cost is lower.

One way of stopping sparking at the controller is to employ a small booster or induction regulator to gradually boost the pressure derived from one tap until it is the same as that of the next tap above. The connection can then be made without danger of short circuiting, and one can pass from tap to tap with a gradually increasing or decreasing voltage. For big installations, where the expense of such an arrangement is justified, there is no doubt that its higher efficiency will commend it. It is possible to arrange the mechanism so that the whole range of voltage is obtained automatically by the mere turning of a handle.

Change-over switches. It is sometimes advantageous to employ taps on the transformer for giving fairly wide ranges of voltage variation, and a booster to give the intermediate voltage values. Where it is desired to give a voltage variation from 410 to 490 volts for lighting, and from 525 to 575 for traction, and where it is important to preserve a good power factor at all voltages, it is a good plan to arrange tappings on the transformer (see Fig. 506), so that on one tapping one can get, without any boosting, 450 volts, the booster being arranged to boost down 10 per cent. and up to 10 per cent., to give the full range from 410 to 490 ; and another tapping giving a normal voltage of 550 without boosting, the booster being employed for obtaining the necessary compounding action. It will generally be found more convenient to arrange the tappings on the high-tension side of the transformer than on the low-tension side ; and where it is not intended to make frequent changes from lighting to traction, ordinary isolating switches are sufficiently convenient for making the change-over. Where, however, it is desired to change-over frequently, inter-connected oil switches can be employed which will throw out one tapping and throw in the other without shutting down the machine. Where the Rosenberg method of starting rotary converters is employed (see page 557), it will be found quite easy to switch over from one voltage to another without shutting down the machine ; all that is necessary is to switch off from the lighting bus-bars,

put in the starting motor, throw over the change-over switch on the high-tension side, and then parallel the machine on the traction bus-bars.

Regulation. When the load on the continuous-current side of an ordinary shunt-wound rotary is suddenly changed, the drop in the voltage which will occur depends upon the amount of drop in the line, the reactive drop in the transformer and the resistance drop in the transformer and rotary. If we maintain the high-tension voltage steady in the sub-station and have a transformer with fairly small reactance drop on full load ($\cos\phi=0$), the voltage regulation will be extremely good (assuming always that the brushes are on the netural point). Only the ohmic drop will then affect the voltage, and this is generally from $2\frac{1}{2}$ to $1\frac{1}{2}$ per cent. in 500-volt machines of 200 K.W. to 1000 K.W. capacity. For machines of low voltage, the ohmic drop is rather greater, on account of the greater drop on the brushes. If the brushes are rocked forward, the drop in voltage will be greater; and if they are rocked backward, it will be less. If there is more reactance in the transformer (say up to 20 per cent.), the drop in voltage on load is somewhat greater, and the machine behaves very like a good shunt-wound C.C. generator. If it is desired to have a very considerable drop on full load, a series coil should be added to the field magnet, connected in such a way as to weaken the field as the load comes on. This is sometimes called a reversed series coil. If then there is some reactance in the transformer, the lagging current drawn from the line causes a drop in the transformer, and the voltage may be made to fall 10 or 20 per cent. as desired (see page 595). A rotary with a series coil connected in this way tends to maintain its load at a steady value, notwithstanding changes in the load of machines running in parallel. A reversed series coil, then, gives stability of load.

Compounding. When it is desired to make the voltage rise as the load comes on, the series coil is connected so as to strengthen the field, and the reactance in the transformer then gives a boosting effect. We give below (page 595) the method of working out quantitatively the voltage rise with a given amount of reactance and over-excitation. Where a rotary is fitted with commutating poles, the effect of rocking the brushes is very much more marked than on a machine without commutating poles. Even without a series winding on the main poles, it is possible to obtain 2 or 3 per cent. over-compounding on the commutating poles only by rocking the brushes back. This method of compounding is not permissible where the rotary has to run in parallel with other rotaries or with C.C. machines, because, there being no equalizer, the load would be unstable, and as soon as the rotary took any load it would tend to take more and more, and probably bring out the circuit-breaker on over load. If, on the other hand, the rotary by chance took current from the line, running as a motor, it would tend to take more and more current to bring into operation the reverse-current mechanism of the circuit-breaker. We get the most stable conditions of running by having the brushes rocked forward of the neutral point. The more we rock them back, the better we make the regulation and the more unstable we make the conditions of running. The rocking of the brushes is the most convenient method of obtaining a fine adjustment of the compounding, and of making a machine share its load with other machines in parallel.

When a rotary is fitted with an A.C. booster, the compounding can be effected by putting series coils on the field magnet of the booster, through which the main

continuous current will pass and strengthen the field of the booster as the load comes on. Sometimes it is an advantage to have series coils on both the main poles of the rotary and on the booster. The effect is then to maintain the power factor as the load comes on, and at the same time to obtain an additional boosting effect.

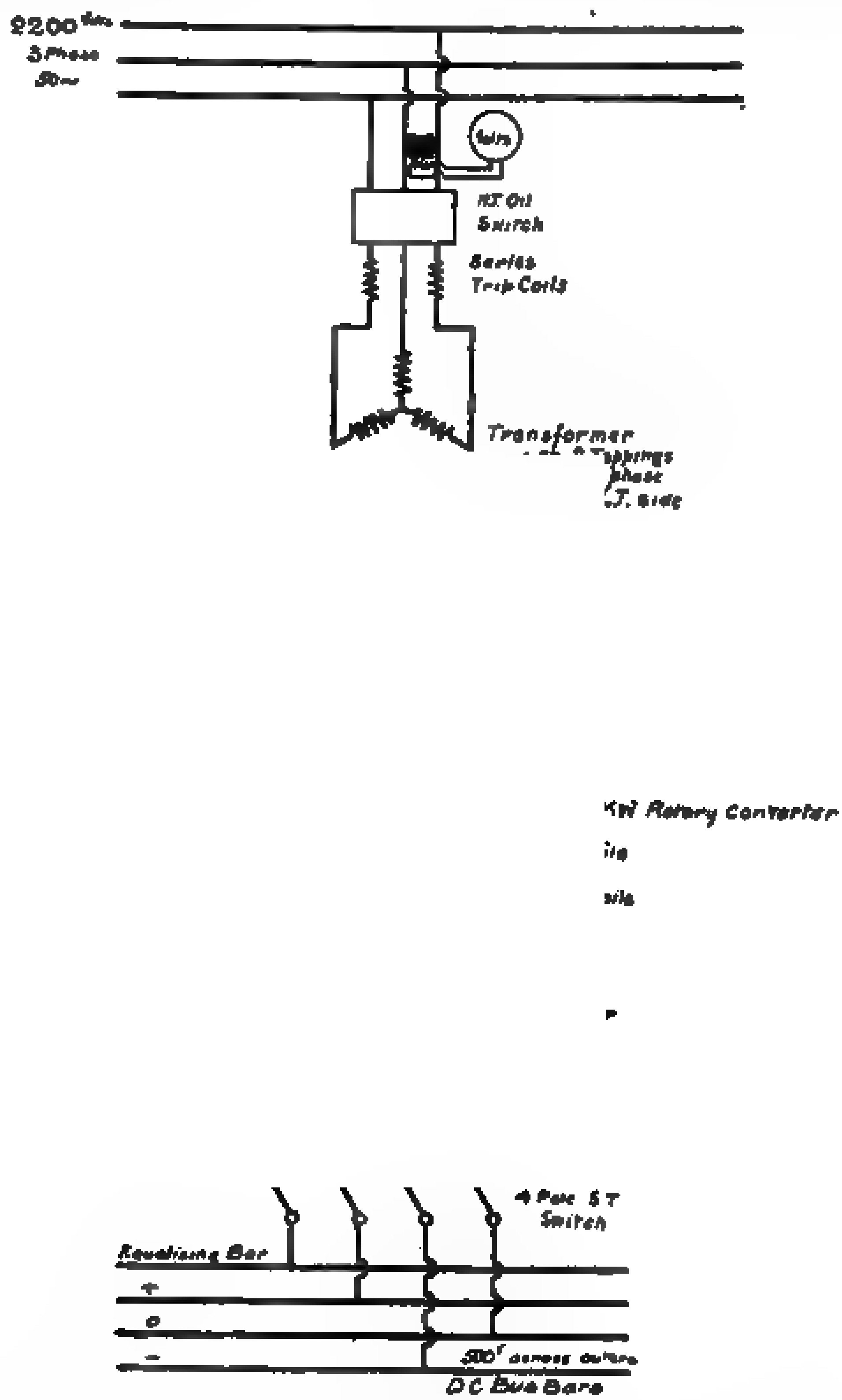


FIG. 507.—Diagram of connection of 3-phase rotary converter, showing method of starting from taps on transformer. Also showing 3-wire connection and equalizer bar. (*Engineering Diary.*)

Equalizing. Where a rotary is over-compounded and it is intended to run in parallel with other compound-wound rotaries or C.C. generators, it is necessary to have an equalizing bus-bar just as with C.C. generators. The resistances of the equalizer bar and connections should be kept as low as possible, the object in view being to feed all series coils from a common source and at one common voltage. Any voltage drop in an equalizer connection which tends to give a machine

a higher voltage on its series winding than exists on the other series windings, will tend to cause instability. Each machine has (particularly if its brushes are rocked forward) a certain amount of inherent stability. Now, the amount of instability caused to any machine by the resistance of the equalizer cable must not be greater than the amount of inherent stability possessed by that machine. Fig. 507 shows the method of connecting to the equalizer bar.

Dependence of C.C. voltage on A.C. voltage. Where the A.C. voltage fluctuates, the C.C. voltage will also fluctuate, the percentage variation being the same on both sides of the machine. In cases where it is desired to maintain the C.C. voltage steady in spite of variations in the A.C. voltage, an automatic regulator may be arranged to control the field of an A.C. booster so as to compensate for the variations; but where the variations on the A.C. side are very sudden, and it is of great importance to keep the C.C. voltage very steady, it is better to use a motor-generator, as in this there is no electrical or magnetic connection between the C.C. side and the A.C. side, and as long as the speed remains constant, the voltage generated is not affected by variations of the A.C. supply.

Three-wire machine. Where a rotary feeds a 3-wire lighting network, it is possible to make it act as a balancer by merely connecting the mid-wire to the star-point of the low-tension side of the transformers. The connections are shown in Fig. 507. In normal cases, the resistances of the rotary and transformer being very small, this balancing effect is exceedingly efficient. A rotary will run with one side fully loaded and the other side unloaded, the whole return current going to the star-point of the transformer. The commutation under these conditions will be quite good, and the drop in voltage on the loaded side need not be more than 3 per cent. Where it is desired to use a rotary as a balancer, and at the same time to compensate for ohmic drop in the mid-wire, a booster may be connected in circuit between the mid-wire and the star-point of the transformer. This mid-wire booster may be either series wound or may carry fine wire winding, the current through which can be controlled by hand.

Power factor. The rotary converter being a synchronous motor on the A.C. side, its power factor can be adjusted by adjusting the excitation of its field-magnet. Its efficiency is highest when running at unity power factor, but it is often desired to make it take a leading current to compensate for lagging currents taken by other apparatus on the system. When it is intended to call for a rotary for this purpose it is better to specify the amount of wattless leading current required than to specify the power factor, for while the leading current may remain constant the power factor will change with the load. Moreover, specifying the amount of leading current required warns the man who draws the specification what he is asking for. To call for a 1000 K.W. rotary converter that shall run on 90 per cent. leading power factor at full load seems a reasonable request, and does not on the face of it appear to involve a much greater expense than to call for a 1000 K.W. rotary to run on unity power factor. But when we remember that this means a machine which must, in addition to its 1000 K.W. load, yield 484 K.V.A. wattless, and that this wattless K.V.A. by itself would cause nearly twice as much heating of the windings as the true kilowatt load, we pause to consider whether as much wattless load is really required. Another matter which needs careful consideration

when calling for wattless leading current is the amount of self-induction that must be put in the transformer to meet other requirements in the specification. Suppose that we call for a rotary without booster that is to be over-compounded 10 per cent. between no load and full load. As will be seen later (see page 596), this will involve the use of a transformer with sufficient self-induction in it to give a reactive drop of about 20 per cent., and the rotary will have to yield a leading current equal to about 0·3 of its full-load current, in order to produce the required boosting effect. This leading current flows between the rotary and the low-tension winding of the transformer. But on the high-tension side of the transformer, the phase of the E.M.F. being different by reason of the self-induction, the current is almost in phase with the E.M.F. That is to say, that while the rotary is yielding a big leading current which is tending to heat it up, this current is not available for compensating for lagging currents in the high-tension system. It is, in fact, absorbed in correcting the power factor of the leaky transformer. Now, if we were to call upon this plant to yield 0·3 of full-load working current leading, in addition to its other load, it would mean that the rotary armature would have to supply a wattless current equal to 0·6 of the full-load working current, and the heating effect would be three times as great as on the same machine (6-phase) working on unity power factor.

Where the converter is required to yield a leading current, and at the same time to have a variable voltage, it is good practice to adopt one of the methods for voltage variation (such as an A.C. booster) which permits the transformer to be built with only a small self-induction. The armature is then not called upon to supply much more than the leading current furnished to the system.

Parallel running. The troubles from hunting which used to occur on the early rotary-converters have been overcome by fitting the poles with well designed dampers or amortisseurs, and by properly adjusting the fly-wheel effect and short-circuit current to prevent resonance with irregularities in the frequency of supply.

Where the angular speed of the generators supplying the power is perfectly uniform, as with turbo-generators, no special precautions are necessary beyond the fitting of suitable dampers to the poles; but where there is a considerable fluctuation in the angular speed in the generators, the amount of the fluctuation and the frequency of the swing should be known, and the fly-wheel effect and short-circuit current of the rotary adjusted to such values that the unsteadiness is not aggravated by resonance. On page 337 the laws governing such matters are given; and on page 345 we have worked out an example to show how resonance may be avoided.

The effect of the damper in reducing the phase-swing is considered on page 601.

Starting. Various methods are used for starting rotaries.

(1) *Starting on C.C. side.* When continuous current is always available in the sub-station where the converter is placed, it is common practice to start up just as one would start a continuous-current motor, a starting resistance being put in circuit at first and gradually cut out as the converter comes up to speed. The speed of the rotary is adjusted by means of the field rheostat. Fig. 506 shows how the connections may be made for a rotary to be started up on the C.C. side. The method of connecting the synchroscope is shown in Fig. 508. According to this diagram, the synchronizing is done on the high-tension side. It may be that at the moment

of synchronism the A.C. voltage from the rotary transformer has not the same virtual value as the A.C. voltage of the mains. If the A.C. switch were closed under these circumstances, the rotary would immediately take load, the amount of which would depend on the difference between the two A.C. voltages at the instant of

FIG. 508.—Diagram of connections of 6-phase rotary converter for supplying power from D.C. to A.C. The machine is started up from the D.C. bars and synchronized on the H.T. side of the transformer. The speed is regulated by the direct-connected exciter. (*Engineering Diary.*)

switching and the regulating quality of the rotary. Under certain conditions, this load might be excessive; so it is good practice, when starting up a rotary on the C.C. side, to open the C.C. circuit-breaker immediately before closing the A.C. switch. Where the C.C. circuit-breaker is fitted with a trip coil, it is easy to arrange for the handle which closes the A.C. switch to make in passing (just an instant before it closes) a contact which brings out the C.C. breaker.

(2) *Starting by means of a starting motor and synchronizing by hand.* This method is very commonly used on rotaries of large capacity, and is generally regarded as a thoroughly satisfactory method. The starting motor is commonly an induction motor with a high-resistance squirrel-cage rotor. The number of pairs of poles is made one less than on the rotary, so that its synchronous speed is higher than that of the rotary, and the resistance of the rotor is arranged to give the required slip, so that the speed of the rotary may be just right when it is fully excited. This enables the A.C. switch to be closed without any shock to the system, as connection is made to an already excited machine. The speed is adjusted by changing the excitation of the main field. This changes the iron loss of the rotary, and hence the amount of slip of the motor. It is permissible to close the A.C. switch when the voltage of the rotary differs by 10 or 15 per cent. from the voltage of the bus-bars, because the machine is disconnected on the C.C. side and nothing happens except the flow of some wattless current. On very large converters loading coils are sometimes provided to change the speed; these are connected to slip-rings and act simply by putting an A.C. load on one of the phases. If preferred, the induction motor may be provided with a wound rotor and slip-rings, and a rheostat used to change the speed.

(3) *Starting from taps on the transformer, self-synchronizing.* It sometimes happens that the A.C. supply to the rotary converters feeding a traction system is cut off for a short time, and all machines are stopped at the same time. When the A.C. supply comes on again, it is necessary to start up the rotaries as quickly as possible and put them into service. Now, it may be that in times of stress such as this the A.C. voltage and frequency are very unsteady; so that just at the time when it is desirable to synchronize quickly it is most difficult to do so. A method of bringing up the rotary to speed quickly and throwing it on the bars without waiting is therefore of the greatest importance. One way of doing this, which is suitable for small rotaries, is by throwing on to the collector rings a voltage of $\frac{1}{4}$ or $\frac{1}{3}$ of the normal voltage, and bringing the rotary up to speed as an induction motor. The dampers on the poles of the rotary in this case act like the squirrel cage in a rotor, and give a very considerable starting torque. The $\frac{1}{4}$ or $\frac{1}{3}$ voltage is obtained by taps on the low-tension side of the transformer; so that even if the machine takes three times full-load current in the armature, the current in the high-tension line has only about full-load value. As the rotary gets near to synchronous speed, the slip is so small that the voltage observed on the brushes alternates very slowly. The wattless currents in the armature magnetize the poles. At first the M.M.F. alternates quickly, but as the rotary comes up to speed the alternation may be so slow that during the time that the salient poles are magnetized with a certain polarity the armature may be dragged into synchronism. When the rotary is up to synchronous speed, the fact is indicated by the C.C. voltmeter, which then gives a steady reading of $\frac{1}{4}$ or $\frac{1}{3}$ full voltage.

A diagram of connections for this system of starting is shown in Fig. 507.

If the polarity of the brushes is correct (as indicated by the polarized voltmeter reading on the right side of the scale), the slip-rings of the rotary can be connected by means of a throw-over switch to a higher voltage, and ultimately to the full voltage of supply. If the machine comes into synchronism with the wrong polarity,

it is necessary to make it slip a pole before throwing it on to the higher taps. This may be done by reversing the connections to the shunt field (by means of a reversing switch shown in Fig. 507). This makes the E.M.F. of the armature oppose the current in the shunt coils, so that the current sinks to zero. As you watch the

Fig. 509.—Diagram of connections for starting a 3-phase converter and self-synchronizing (Rosenberg's method).

C.C. voltmeter, you see the needle swing to zero as the field dies and the armature begins to slip. If, now, the reversing switch be thrown over again (so as to come into its normal position), just as the armature has slipped one pole, the field will excite again; but it is now found that the polarity is right.

This method of starting a rotary is the simplest, and would be satisfactory but for two drawbacks: (1) the large wattless current taken from the line, and (2)

the sparking which occurs under the brushes during starting. This sparking may be very troublesome, and even where it is slight it may be sufficient to prevent the commutator from assuming that high state of polish which is so desirable. For this reason some firms provide apparatus attached to the brush gear to raise the brushes at starting. For small machines this is not generally regarded as necessary, and as the current drawn from the line is not so excessive, the method is very widely used for small rotaries up to say 350 K.W. capacity.

Sometimes two sets of tappings on the low-tension side of the transformer are provided, so that the voltage applied to the rings can be brought up on easy stages and the rush of current which occurs on throwing over to the higher tappings is reduced.

Fig. 507 shows the arrangement for starting a small three-phase rotary by means of two double-throw triple-pole switches connected to taps on the transformer.

(4) *Starting motor connected in series with slip-rings.* Dr. E. Rosenberg has introduced an ingenious method of starting and synchronizing rotaries which possesses the advantage of rapid self-synchronizing without the disadvantage of taking heavy wattless currents from the line or causing sparking at the brushes. The method will be understood from Fig. 509, which gives the connections for a three-phase rotary. A three-phase starting motor has the six ends of its star winding brought out. The ends which would be ordinarily starred are connected to three of the rings of the rotary. The impedance of the winding of the rotary armature is so low that for practical purposes these ends may be regarded as star connected at the moment of starting. The other three ends of the motor winding are carried to the terminals of the transformer *A*, *E*, and *C*. The three-pole low-tension knife switch shown in the figure is open during the starting. In order to start, the motor switch is closed and the motor brings the rotary rapidly up to speed, taking only about 30 per cent. of full-load current. As the voltage across the rings at starting is quite low, perhaps 6 per cent. of normal voltage, there is no sparking at the brushes. As the rotary gets up speed it excites itself, but as long as the frequency of its alternating voltage is different from that of the supply the motor still exerts a turning moment, though this will be less or greater according as the voltage of the rings is in or out of phase with the supply voltage. The motor is wound with one pair of poles less than the rotary, so that it would take it above synchronism if it were not for the fact that the current through the starting motor provides enough torque on the rotary acting as a synchronous motor to prevent it from exceeding the synchronous speed. The condition of synchronism is indicated by a steady reading on the central-zero voltmeter.

If the starting current is kept fairly low, say 30 per cent. of full-load current, the residual magnetism of the rotary will not be disturbed, and the polarity of the terminals will be right when the machine gets into step. If it is desired to start the rotary in a very short time, say 20 seconds, a somewhat larger starting current must be used, and then to ensure the rotary having the correct polarity when going into step a field switch is provided which is kept open until synchronous speed has been almost reached. By watching the voltmeter which moves a little to the right and left as the rotary slips pole after pole, a moment can be chosen for closing the

field switch so that the polarity will come up in the right way. Fig. 510 shows the arrangements for a six-phase rotary converter.

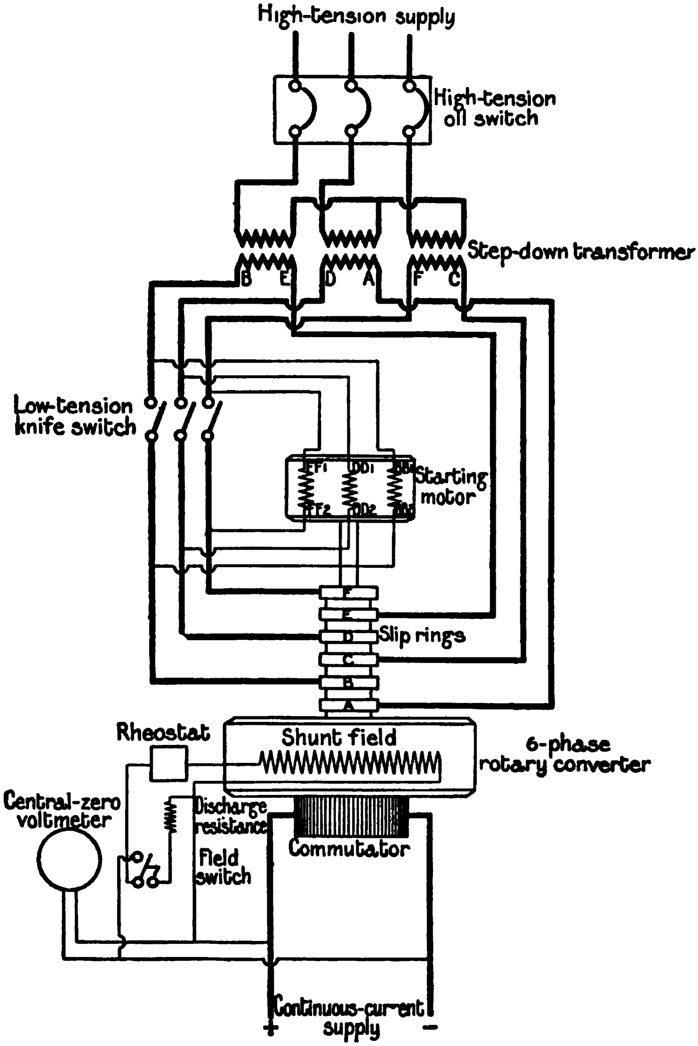


FIG. 510.—Diagram of connections for starting 6-phase converter and self-synchronizing (Rosenberg's method).

Running C.C. to A.C. Sometimes it is required to take power from continuous current bus-bars and convert it into A.C. power for transmission to some distant point. The rotary converter is very widely used for this purpose on account of its high efficiency. Where the converter employed for this purpose has to run in parallel with A.C. generators of definite frequency, no special precautions need be taken to regulate the speed of the converter, because its speed will be synchronous with that of the A.C. generators. Where, however, there is no

generator of definite frequency in parallel with the converter, it is necessary to regulate the speed and so fix the frequency of supply.

One difficulty in fixing the speed of the converter when it is running without any synchronous generator in parallel arises from the fact that any wattless load (current lagging) tends to weaken the field magnet of the converter, and thus to increase the speed. An increase of speed makes the current lag more, and thus one gets a cumulative effect that may result in the converter running away.

In order to obviate this difficulty, Mr. B. G. Lamme proposed the use of an under-saturated exciter driven by the converter. The exciter for this purpose is generally mounted on the end of the shaft of the converter, and its armature is electrically connected through a rheostat to the shunt winding of the converter. The exciter is so designed that at the voltage at which it ordinarily works the iron of the magnetic circuit is not saturated, that is to say, the normal-voltage point is below the knee of the saturation curve. Under these circumstances, a slight increase of speed of the converter makes a very considerable increase in the exciting current. It is, in fact, possible under practical conditions to obtain an increase of 5 per cent. in the exciting current for an increase of only 1 per cent. in the speed. This arrangement is found to work very well, so that even with a varying load of low power factor, the speed of the converter can be kept within sufficiently narrow limits. Where heavy over loads of low power factor are expected, it is well to put series coils on the converter arranged so that when a load comes on the field of the converter is strengthened. This plan is sometimes adopted in small converters instead of using the direct-driven exciter. It is effective in those cases where the wattful load increases at the same time as the wattless load.

After these observations upon matters affecting the operation of rotary converters in general, we can proceed to give model specifications such as might be issued by the engineer of the intending purchaser.

We shall consider two cases: First, a 1250 K.W. 6-phase converter for lighting and power supply at 460 to 500 volts, as well as for traction work at 525 to 560 volts. This machine will be fitted with an A.C. booster, so that we can consider the characteristics of such a generator to work out its design in detail. Secondly, a 2000 K.W., 250-volt, 6-phase rotary designed for electrolytic work.

We shall then give some notes on the methods to be adopted to meet special requirements.

SPECIFICATION No. 14.

1250 K.W. ROTARY CONVERTER AND A.C. BOOSTER.

(General Clauses Nos. 1, p. 269; 21, p. 333; 170, p. 519.)

Extent of Work.

214. This specification provides for the supply, erection, testing and setting to work of a rotary converter and A.C. booster set, having the following characteristics:

Characteristics of Rotary Converter.

Characteristics of Converter.

215. Normal output:

Running A.C. to C.C.	1250 K.W.
Running C.C. to A.C.	1250 K.V.A. at 0·9 power factor.
Number of phases	6.
Frequency	50 cycles per second.
Continuous-current voltage	460 to 500 volts for lighting bus-bars. 525 to 550 volts for traction bars.
Continuous-current amperes	2360 amperes.
Three-wire network	Rotary converter to act as a balancer. Out of balance current 600 amperes.
Compounding	On traction 525 to 550.
Adjustment of voltage on rheostat:	
A.C. to C.C.	On lighting from 460 to 500 while H.T. volts vary from 6000 to 6400.
C.C. to A.C.	6400 to 6600 while the lighting bus-bars vary from 460 to 500.
Range of variation of A.C. voltage	In practice this may vary between 6200 and 6700, but performance is only asked for on the basis of 6400 to 6600.

Leading idle power required from rotary and transformer when running A.C. to C.C. at full load 490 to 550 volts	300 K.V.A.
Over load	25 per cent. for 3 hours at unity power factor. 50 per cent. for 10 minutes at unity power factor.
Temperature rise after 6 hours full load at 550 volts, 0·97 power factor on H.T. side	40° C. by thermometer.
Temperature rise after 3 hours 25 per cent. over load	50° C.

CHARACTERISTICS OF BOOSTER.

215*a*. The A.C. booster shall have a revolving armature situated between the slip-rings and the converter armature. Its capacity shall be sufficient to enable the continuous-current voltage of the converter to be changed gradually at any load from 460 to 550 when the high-tension voltage in the sub-station has any value between 6400 and 6600. The change of voltage may be effected by boosting down in the lower part of the range and boosting up in the upper part of the range; but in proceeding step by step from the lowest voltage to the highest, there must be a continuous action of the controlling handle.

Characteristics of Booster.

216. During the whole range of voltage, the power factor of the set on the high-tension side shall be under control,* so that it can be adjusted at any voltage between the limits of unity and 0·97 leading, by altering the main field rheostat.

Power-factor Control.

* Where it is not necessary to control the power factor, a somewhat cheaper arrangement may be suitable. In this case, the clause as to power-factor control might read as follows:

It is not insisted that the power factor of the set shall be completely under control throughout the whole range of voltage; it may be lagging (not less than 0·9 at full load) between the voltages of 460 and 480, and may be leading between the voltages of 530 and 550. Between the voltages 480 and 530, which are the normal voltages on lighting and traction respectively, it shall be possible to maintain the power factor at unity, and at the higher voltages it may be leading.

Duty of Plant. 217. Two rotary converters of the above rating, with their boosters and transformers, are required to run in an existing power-station for feeding into the low-tension network in the vicinity of the power-station, and also to form a link between the existing A.C. generators and C.C. generators. The generators consist at present of three 3000 K.W. three-phase steam-turbine-driven sets running at 1500 R.P.M., and two 2000 K.W. continuous-current generators driven by direct-connected steam engines running at 120 R.P.M. Normally, one of the 1250-K.W. rotary converters will feed the lighting bus-bars, and for this purpose taps must be provided on the high-tension side of the transformer to enable 480 volts C.C. to be obtained at unity power factor without any appreciable boosting. The other will run normally on the traction bars, and taps must be provided on the transformer, which give (at unity power factor) 530 volts without any boosting.

Interchangeability. 218. Both sets are to be completely interchangeable.

Running Inverted. 219. On Sundays, or at such other times as it may be desired to shut down some or all of the A.C. turbo-generators, the two converters are to be capable of running inverted from the C.C. generators in the station and of supplying A.C. power through their transformers at from 6300 to 6600 volts to outlying districts. It may be that one or two rotaries will have to supply the whole of the A.C. power; or it may be that one or both will have to run in parallel with one or two of the A.C. generators, assisting in the supply of A.C. power.

Maintain Frequency. 220. When running as the only source of A.C. power, they shall maintain the frequency within 5 per cent. of 50 cycles per second, provided the wattless K.V.A. does not amount to more than 300.

Variation of Load. 221. When running on the traction bus-bars, the current may vary from 1000 amperes to 3000 amperes from minute to minute; the machine must therefore be of liberal design and commutate well during the peaks of the load.

Run well in Parallel on C.C. Side. 222. It must run well in parallel with one or two of the present 2000 K.W. C.C. machines, which will be compound-wound, when running on the traction bus-bars.

223. The voltage drop in the series windings and connections at full load on these machines is 2·5 volts. The Contractor must provide all diverters and series resistances necessary to make the rotary converters divide their load reasonably well with the C.C. generators. The compounding of the present generators is from 525 volts at no load to 550 at full load. **Voltage Drop in Diverters.**

224. When running in the lighting bus-bars, the rotary converters must have the characteristics of shunt machines with 9 per cent. drop in voltage between no load and full load. **Characteristics of Shunt Machines.**

225. The converter must be capable of acting as a balancer, and of dealing with an out-of-balance current of 600 amperes in the middle wire. With this current flowing, the difference in voltage between the two sides of the three-wire system shall not be more than 1 per cent. of the voltage across the outers. **Balancer.**

226. When running on light load during some parts of the day the rotaries are to be over-excited and to take a leading current from the line. When running at quarter load, measured on the C.C. side, each machine must be able to supply 500 K.V.A. wattless; and when running on full load each must be able to supply 300 K.V.A. wattless for six hours without exceeding 40° C. rise. **Leading Wattless Load.**

227. The voltage of the high-tension bus-bars varies between 6300 and 6600 volts. The design of the rotary converters must be such that this change of pressure will not cause them to take such excessive loads as to bring out the circuit breakers or cause trouble from bad commutation. **Change in H.T. Voltage.**

228. Under all the conditions set out above, the converters must be very stable and free from hunting. **Stability in Operation.**

229. The rotary converters must run without any appreciable sparking or glowing of the brushes while the load is changed from zero to 25 per cent. over load, with the brushes in a fixed position. **Commutation.**

230. Each converter may be started up by means of a starting motor or other means, but the arrangements shall be such that in case of emergency it can be switched in on the A.C. side in less than 1½ minutes from the time of starting from rest, however unsteady the frequency and voltage may **Emergency Starting.**

be, and even if the voltage is only 90 per cent. of its normal value. When switched on it must be of right polarity and immediately available for throwing on the C.C. bus-bars, provided always that the C.C. voltage is high enough. In the case of this emergency starting, it will be permissible to draw from the line a momentary current equal to 1·5 times full-load current, but there must be no necessity to wait for any indication on any instrument or any synchronizing which is not perfectly automatic.

Normal Starting.

231. Preference will be given to methods of starting which, while complying with the above requirements for starting on an emergency, can be so ordered under normal conditions that there is no shock to the system on throwing in a machine. For this purpose the machine may be synchronized either by hand or automatically, and the time taken may be dependent upon the steadiness of the frequency and voltage.

Vibration and Noise.

232. The sets must run smoothly and without vibration under all conditions of load. They must produce no more noise than is made by machines of similar size and speed constructed according to the best practice in this respect.

Insulation Tests.

233. The armature windings and field windings of the rotary converters are to be subjected to a pressure test of 2000 volts alternating, applied between the windings and frame for one minute while the machine is hot.

Puncture Test on Site.

234. This test shall be carried out at the maker's works in the presence of the representative of the Purchaser, and it shall be repeated after the plant is installed if, in the opinion of the Purchaser, there is reason to believe that the windings have been damaged.

Efficiency.

235. The efficiency of the rotary converter shall be calculated from the separate losses, which shall be measured in the following way :

(*a*) *Iron loss, friction and windage.* The machine shall be run as a C.C. motor at full speed and at various voltages, and measurements made of the C.C. power taken to drive it, and of the exciting current taken at various voltages. These tests shall be taken both with the booster fully excited and with the booster unexcited.

(*b*) *Copper losses.* The resistance of the armature and series field coils of the rotary converter and booster shall be taken by measuring the voltage drop in them when a substantial current is passed through them at a known temperature. The resistance on full load shall be taken as 1·2 times the resistance at 20° C. The loss in the converter armature resistance at 0·96 power factor shall be taken to be 0·378 times the loss on the armature when working as a continuous-current generator. The resistances of the series coils and commutating winding shall be taken with any diverters that may be necessary, suitably attached and adjusted.

(*c*) The losses in the *shunt windings* of converter and booster and their rheostats shall be taken to be the exciting current multiplied by the voltage of excitation in each case respectively, except that where any potentiometer-type rheostat is employed, the total current going to the rheostat and field shall be taken as the exciting current.

(*d*) The brush contact losses on the commutator shall be calculated by multiplying the measured combined pressure drops at the positive and negative brushes by the continuous current. The brush contact losses on the slip rings shall be calculated by multiplying the measured pressure drop from brush holder support to slip rings by the current per ring and the number of rings.

236. The Contractor shall state what efficiency * he is prepared to guarantee at full load, three-quarter load and half load, the efficiency being calculated as stated in the last clause. **Guarantee of Calculated Efficiency.**

Or (see Clause 250, page 586).

Or,

237. The Contractor shall state what efficiency he is prepared to guarantee at full load, three-quarter load and half load, such efficiency to be measured by means of both indicating and integrating instruments on the A.C. and C.C. sides. **Guarantee of Measured Efficiency.**

238. The instruments used in the measurement of the efficiency specified in Clause 237 shall be calibrated both **Calibration of Instruments.**

* Very commonly, the converter and its transformers are supplied under the same contract, and in that case it is usual for the Contractor to give figures for the overall efficiency of the whole set.

before and after the test by some institution, to be agreed upon by the Contractor and Purchaser.

Provision of Instruments and Power for Tests.

239. All instruments required for the measurements aforesaid shall be provided by the Contractor, and all power required for one preliminary test and one final test shall be provided by the Purchaser, free of charge. If either party shall require a test to be repeated, the party so calling for a repetition shall pay for the power consumed, unless it shall appear that he was justified in calling for a new test and that the necessity for it was not due to his fault. Power for additional tests shall be supplied by the Purchaser at the rate of   per unit.

Tests. (See Clause 284, p. 592.)

Terminals and Connection. (See Clauses 44, p. 361; 112-113, p. 443; 199, p. 525; 267, p. 590.)

Bearings. (See Clauses 67, p. 380; 268, p. 590.)

Brush Gear. (See Clauses 106, p. 442; 188, p. 523; 191, p. 524; 263, p. 589; 305, p. 609; 312, p. 611.)

Oscillator. (See Clause 264, p. 589.)

Insulation. (See Clauses 93, p. 439; 269, p. 590.)

Sample Coil. (See Clause 270, p. 590.)

Grinding Gear. (See Clause 271, p. 591.)

Painting. (See Clauses 209, p. 528; 278, p. 591.)

Cables. (See Clauses 6, p. 271; 42, p. 361; 73, p. 382; 279, p. 591; 320, p. 611.)

Spare Parts. (See Clauses 20, p. 274; 114, p. 444; 169, p. 503; 200, p. 525; 280, p. 592.)

Dates of Completion. (See Clause 281, p. 592.)

Drawings. (See Clauses 174, p. 519; 282-283, p. 592.)

Foundations. (See Clauses 6, p. 271; 36-37, p. 360; 74, p. 382; 272, p. 591.)

Use of Crane. (See Clauses 8, p. 271; 60, p. 379; 273, p. 591.)

Accessibility of Site. (See Clauses 8, p. 271; 55-59, p. 379; 125, p. 461.)

Screw threads. (See Clause 277, p. 591.)

DESIGN OF A 1250 K.W., 50-CYCLE, 6-PHASE ROTARY CONVERTER TO COMPLY WITH SPECIFICATION NO. 14.

Voltage variation. In designing any rotary converter, the first question to consider is the method by which the variation of voltage is to be carried out, because the power factor will depend upon the method we employ (see page 546).

In this particular case the voltage is to be varied from 460 to 500 volts on a lighting load, and from 525 to 550 on a traction load. If there were no objection to operating the set at a low lagging power factor on the low voltages, and a leading power factor on the higher voltages, the variation of voltage might be carried out by means of an inductance in the transformer, in the manner described on page 595. But in this case the purchaser requires the converter to yield 300 leading wattless K.V.A. at all loads, so that it is not permissible to run the converter on a lagging power factor. The most suitable method, therefore, for obtaining the voltage variation is by means of an A.C. booster, which by preference should be mounted on the shaft of the converter between the slip rings and the armature.

Having adopted the booster method of voltage variation, the only necessity for wattless current will be the meeting of the guarantee to deliver 300 leading wattless K.V.A. on the high-tension side of the transformer.

Taking into account the magnetizing current of the transformer, which in this case would be supplied by the converter and would amount to about 5 per cent. of the full-load current, the power factor on the low-tension side of the converter would be 0·96 leading. If the efficiency of the converter at full load be 96 per cent., the K.V.A. input would be about 1360. Referring to the curve in Fig. 504, we see that the loss in the armature conductors will be 0·33 of the loss in an equivalent C.C. generator, but taking all the factors into account which are considered on pages 544 and 545, we know that the temperature rise of the winding will be about 0·378 of the temperature rise of an equivalent C.C. generator.

Under these conditions, experience shows us that we may take a D^2l constant of about 2×10^5 (see page 570).

Number of poles. When we are designing a 500-volt rotary converter intended for traction work, our main aim will be to produce a machine having highly satisfactory commutating qualities even when carrying a heavy over-load. For this reason, the number of kilowatts per pole should be made much smaller than would be permissible in a converter intended for a steady load. A rating of 100 K.W. per pole may be taken as a fairly conservating figure, and some designers might prefer only 80 K.W. per pole.

In the old days designers were cautious, and knowing that the commutation was easier when the current per brush arm was small, they built their converters (particularly the high-frequency converters) with a large number of poles. This gave very large machines of slow speed, and though the performance was fairly satisfactory the efficiency was much lower than it need be and the cost was high. It was soon found that with proper adjustments much larger currents could be satisfactorily dealt with on each brush arm, so the number of poles was decreased, the speed increased, with the result that we have now very much cheaper and

more efficient machines. How far this reduction in the number of poles will go in the future it is difficult to say. It is quite possible to build a 1500 K.W., 550-volt, 6-phase, 50-cycle rotary converter, having only 4 poles, and running at 1500 R.P.M., but such a machine would not be cheaper or more efficient than a six-pole machine running at 1000 R.P.M. It is doubtful whether the six-pole machine would be an improvement upon the slower speed machines with which we are more familiar; the commutator would be very long, and we should have conditions such as we have in continuous-current turbo-generators, instead of the easier conditions of engine-type machines. The saving in cost as we reduce the number of poles is not as great as we might at first suppose. The commutator is one of the most costly parts of a converter, and we gain nothing in economy by reducing the diameter, for we have to make it longer in proportion (or even in a greater ratio) if we have to deal with the same current. The bars on a long commutator, moreover, are deeper than those on a short commutator of the same peripheral speed, so the cost of the commutator is really increased as the number of poles is reduced. We do not get so much benefit on a long commutator from the blowing of the commutator necks. Now there is nothing to be saved on the collector gear by increasing the speed of the converter, for we have the same current to collect. As we have to provide for a certain cooling surface on each ring for each watt lost, the conditions are only made more difficult with increased speed. The considerations as to commutator brush gear and collector are of great importance, because in a sense these are the most essential features in a rotary converter. We can imagine the other parts of the machine being done away with.

Weight of copper on the armature. For the same peripheral speed a greater weight of copper is required when the poles are few than when they are many. The reason is, that we have in any case the same volume of current to carry from the slip-rings to the commutator, but when the number of poles is greater the number of paths in parallel is greater, and therefore the current per slot is smaller, which enables us to work at a rather higher current density.

We do, however, make a saving in the iron of the magnetic circuits and in the size of the frame by reducing the number of poles. The relative calculated costs of commutators, armature windings, armature punchings and frames will be very different in different factories. In getting out costs, so much depends upon the rating of the tools used and the apportionment of general charges. It is therefore impossible to give any rule for arriving at the best number of poles for a rotary converter of given output.

In the example worked out below, we have taken 14 poles. This number is suitable, judging from the general practice of to-day.

It gives a rating of 90 K.W. per pole. As the machine is rated at 2360 amperes, we have $2360 \div 7 = 336$ amperes per brush-arm at normal load, and 500 amperes per brush-arm at 50 per cent. over load. These are suitable values for a 50-cycle converter subjected to heavy fluctuating loads. On a 25-cycle converter we would work at a higher current per brush-arm in order to reduce the number of poles and increase the speed.

On low-voltage machines, where the current to be generated is very great, one is guided more by amperes per brush-arm than by kilowatts per pole. We might,

for instance, take 1000 amperes per brush-arm as a maximum beyond which it is not desirable to go, and fix the number of poles accordingly. But on a 500-volt machine the current per brush-arm is usually kept lower than this.

Diameter and length. These will generally depend upon a manufacturer's standard sizes; but if they are to be settled from first principles they would be controlled by the choice of a suitable pole pitch. This, in a 50-cycle converter, may conveniently lie between 30 and 36 cms., and really depends upon the room required for the requisite copper and iron, without exceeding an axial length which has been found in practice to give good commutation. If we choose a pole pitch of 32 cms. in this case, the circumference of the armature will be $32 \times 14 = 448$ cms. Taking a diameter of armature 142 cms., we will find that we can get in the requisite copper and iron without making the axial length greater than 31 cms.; so that this diameter is suitable. These dimensions make the output coefficient $= 2 \cdot 1 \times 10^5$. Before we can calculate K_e, we must settle upon the pole arc. The main consideration in settling this is to leave enough room for the commutating pole and space between the commutating pole and main pole. On 50-cycle converters, the space required for these generally amounts to about 25 per cent. of the pole pitch; so that the pole arc ought not to exceed 75 per cent. of the pole pitch. In this case our pole arc is 23·5 cms.; and as the tips of the poles have a slight bevel, the coefficient K_e (see page 13) equals 0·74.

Number of bars per pole. A machine of large output of this kind will be invariably wound* with a lap winding, there being one turn per commutator bar. The number of bars will be settled from considerations similar to those given on page 532, and we may take 48 bars per pole as an entirely satisfactory number; so that we shall have 96 conductors in series between the positive and negative brushes. Allowing 15 volts drop in the resistances of transformer and converter, we arrive at the formula:

$$565 \text{ volts} = 0 \cdot 74 \times 7 \cdot 15 \text{ revs. per sec.} \times 96 \times A_g \mathsf{B} \times 10^{-8},$$

$$A_g \mathsf{B} = 1 \cdot 11 \times 10^8.$$

We can now make a general check calculation to see the relation of the $A_g\mathsf{B}$ and the $I_a Z_a$ to the size of the frame. The calculation sheet is given on page 570. The circumference $= 446$ cms., and the area of the working face $A_g = 13{,}800$ sq. cms.

$$1 \cdot 11 \times 10^8 \div 13{,}800 = 8000 \text{ C.G.S. lines in the air-gap},$$

$$168 \times 1344 \div 446 = 500 \text{ ampere-wires per cm. of periphery.}$$

These values are suitable. The length of armature is now settled by the amount of iron required in the teeth. Figs. 511 to 516 give sectional views of the machine. Fig. 513 shows the size of slot and the arrangement of the conductors.

The flux-density in the teeth. This will depend upon the relative importance of securing a high efficiency and of building a cheap machine. There is no doubt that very high flux-densities can be employed without making the temperature

* Small converters up to 200 K.W., 500 volts are generally wound with wave-windings with two circuits in parallel. In rare cases it is desirable to use the Arnold singly re-entrant winding described on page 512. In these cases the best way of finding the points on the winding to which the slip-rings are connected is the method described by Dr. S. P. Smith and R. S. H. Boulding in the *Journ. I.E.E.*, vol. 53, p. 232.

Date 8th Mar. 1914. Type ~~GEN.~~ SYN ~~MOTOR~~ ROTARY 14 Poles Elec Spec 14
K.W. 1250; P.F. ; Phase 6; Volts 460–550; Amps per ter. 2360; Cycles 50; R.P.M. 428; Rotor Amps
H.P. ; Amps p. cond. 168; Amps p. br. arm. 336; Temp. rise 40°C; Regulation Comp 320/330; Overload 25%

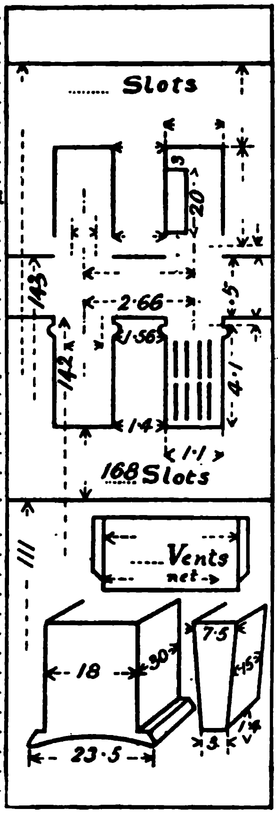
Customer ; Order No. ; Quot. No. ; Perf. Spec. ; Fly-wheel effect

Frame 142/31; Air ; Circum. 446; Gap Area 13,800; poss. $A_g B$; $A_g B$ 1.11 × 10⁸; poss. $I_a Z_a$; $I_a Z_a$ 226,000; $I_a Z_a$ Circum. 500; $\frac{D^2 L \times RPM}{K.V.A.}$ 2.1 × 10⁵

K_a .74 ; 565 Volts = .74 × 7.15 × .96 × 1.11 ; Arm. A.T. p. pole 7000 eff. ; Max. Fld. A.T. 8330

Armature. Rev. ~~Stat.~~

Core	Dia. Outs.	142	cms
	Dia. Ins.	111	
	Gross Length	31	
	Air Vents 4 × .65	2.6	
	Opening Min. Mean		
	~~Air~~ Velocity	32	
	Net Length 28.4 × .89	25.2	
	Depth b. Slots	11.5	
	Section 290 Vol.	110,000	cu. cms
	Flux Density	10,000	
	Loss .05 p. cu. cm. Total	5500	12300
	Buried Cu. 2700 Total	9600	
	Gap Area 13800; Wts	5000	
	Vent Area 38,000; Wts	4750	
	Outs. Area 21,000; Wts	3100	12,850
Teeth	No of Segs Mn. Circ.	430	
	No of Slots 168 × 1.1 =	185	
	K_s = 2.2	245	
	Section Teeth	6200	
	Volume Teeth	27,500	
	Flux Density	18,000	
	Loss .148 p. cu. cm. Total	4100	
	Weight of Iron	1070	kgs
Conductors	Star or Mesh Throw	1–12	
	Cond. p Slot	8	
	Total Conds 96 in Series	1344	
	Size of Cond. .2 × 1.3	0.26 sq. cm.	
	Amp. p. sq. cm	640	
	Length in Slots 31		
	Length outside 47 Sum		
	Total Length 78	1050 m.	
	Wt. of 1,000 232 Total	243 kgs.	
	Res. p. 1,000 .654 Total	.686/196	.0035
	Watts p. metre 90		
	Surface p. metre 840		
	Watts p. Sq. .107 × 13 / .0012	.107 / 11.5° C	



Field Stat ~~or Rotor~~

~~Dia.~~ Bore	143		
½ Total Air Gap	0.5		
Gap Co-eff. K_g	1.16		
Pole Pitch 32 Pole Arc	23.5		
K_f	.74		
Flux per Pole 5.7 × 10⁶			
Leakage n.l. 1.2 f.l.	6.9 × 10⁶		
Area 530 Flux density	13000		
Unbalanced Pull			
No. of Seg. Mn. Circ.			
No. of Slots × =			
Vents			
K_s Section			
Weight of Iron Poles	1800 kgs		

	Shunt.	Series.	Com.
A.T. p Pole n. Load	3950 to 4230		
A.T. p. Pole f. Load	6330	1800	4720
Surface	71,000		
Surface p. Watt	18.7		
I^2R	3800	1300	1900
I. R.	390	.8	
Amps. 5 to	9.7		
No. of Turns	650	1	2
Mean l. Turn	110	140	60
Total Length	10,000	20	17
Resistance	39 hot	.0004	.0003
Res. per 1,000	3.28	.017	.017
Size of Cond.	.26 rd .053	10	10 sq. cm
Conds. per Slot			
Total Length			
Wt. per 1,000	47	8900	8900
Total Wt.	470	180	150 kgs
Watts per Sq.	.053		
Star or Mesh			
Paths in parallel	1	1	1

Magnetization Curve.

	Section.	Length	500 Volts. B.	A.T. p. cm	A.T.	550 Volts. B.	A.T. p. cm	A.T.	580 Volts. B.	A.T. p. cm	A.T.
Core											
Stator Teeth											
Rotor Teeth	6200	4.1	16,000	35	143	17,500	85	350	18,500	140	575
Gap	13800	.5	7000		3280	7830		3600	8250		3800
Pole Body	530	27	11,500	5	140	12,700	7	190	13,600	9	240
Yoke	515	30	6100	2.6	80	6700	3	90	7100	3.4	100
					3643			4230			4715

Commutator

Dia. 112 Speed 25 m p s
Bars 672
Volts p. Bar 16
Brs. p. Arm 6
Size of Brs. 2 × 4.5
Amps p sq. cm 6.1
Brush Loss 4700 + 4200
Watts p. Sq. 11300 / 8700 = 1.3

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.	21	21	21	21	21
Iron Loss	9.6	9.6	9.6	9.6	9.6
Field Loss	3.3	3.3	3.3	3.3	3.3
Arm. &c. I^2R	18.3	11.7	6.6	2.9	.7
Brush Loss	8.0	6.4	4.8	3.	1.0
	60.2	52.0	45.3	39.8	35.6
Output	1560	1250	938	625	313
Input	1620	1302	983	665	349
Efficiency PF = .96	.963	.96	.953	.94	.896

Mag. Cur. Loss Cur
Perm. Stat. Slot
„ Rot. Slot × = 1.48
„ Zig-zag .57
2 × × =
1.77 × × =
End × × = .85
Amps Tot. 2.9
τ = , X_a =
S_1/S_2 , r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor To yield 300 K.V.A. leading at full load

too high; but if we consider the cost of power lost in the teeth we shall find in most cases that it will pay, as an engineering proposition, to slightly increase the size of the machine, so as to work at a lower density in the teeth and make a saving in power. A density of 18,000 C.G.S. lines per sq. cm. is generally satisfactory at 50 cycles. Where efficiency is specially important, a lower figure will be chosen, and where efficiency is of less importance, a higher figure. Dividing $1{\cdot}11 \times 10^8$ by 18,000, we get 6200 sq. cms. for the cross-section of all the teeth.

Number of slots. The fewer the numbers of conductors per slot on a rotary converter and the greater the number of slots, the better from the commutation point of view. From considerations of economy, however, we find it necessary to group six or eight conductors in one slot on a 500-volt machine. Eight conductors per slot gives quite good commutation conditions where proper care is taken in the design of the commutating pole. We therefore choose this number, and arrive at $96 \div 8 = 12$ slots per pole.

Size of conductor. This will depend upon the power factor at which the converter is intended to work. It is only by actual trial under the ventilating conditions which obtain on any given machine that we can with certainty state the load which can be carried by a conductor of a certain size at a certain power factor. The considerations which determine the size of armature conductor on a converter are given on page 544. Where the power factor is lower than unity, the heating of the conductors near the point where the taps are connected is very much greater than the heating of intermediate conductors; and the rate at which the heat is conducted from the hot parts of the armature to the other parts is so uncertain that no exact calculation is possible. It is found, however, that on 50-cycle converters having a peripheral velocity of 30 metres per second, and with the means of ventilation ordinarily available, one can, when the power factor is near unity, work as high as 900 (nominal) amperes per sq. cm.; that is, taking the current as that of a C.C. generator.

In actual practice, it is seldom found advisable to work the copper at such a high current density as to bring up the temperature to the guaranteed temperature, because by so doing we should only be saving a small weight of copper at the cost of considerable loss of power. The amount of material employed will depend upon the efficiency which must be obtained. In the case under consideration, if we have regard only to the mean temperature rise, we see from page 545 that at a power factor of 0·96 the heating will be about 0·378 of what it would be on a continuous-current generator. The current density, therefore, may be made $\frac{1}{\sqrt{0{\cdot}378}} = 1{\cdot}62$ times as great. This would give us a possible current density of 750 amperes per sq. cm. (nominal), and a total loss of 10,000 watts in the armature resistance (see page 544). If we use a slightly greater section of copper so as to work at 640 amperes per sq. cm. (nominal), we will reduce the copper losses by 1500 watts, and at the same time run less risk of exceeding the temperature guarantees at the points of the winding near the taps. In checking the mean temperature rise of the copper above the outside of the insulation, we have the following expressions (see page 570), $0{\cdot}000654 \times 1{\cdot}16 \times 168 \times 168 \times 0{\cdot}378 \times 8 \times 1{\cdot}4 = 90$ watts per metre length of coil. The cooling surface per metre length of coil is 840 sq. cms.,

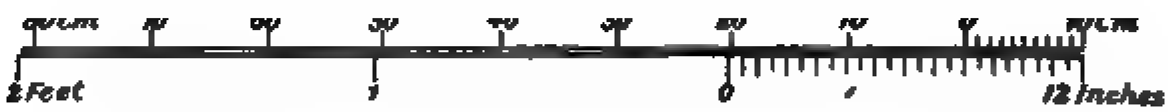


FIG. 511.—Sectional drawing of 1250 K.W. 6-phase rotary converter, 460-550 volts, to meet Specification No. 14, page 560.

FIG. 512.—Sectional drawing of A.C. booster mounted on shaft of rotary converter to comply with Specification No. 14.

giving 0·107 watt per sq. cm. As the thickness of the insulation is 0·13 cm. and the conductivity 0·0012 (see page 225), we have

$$\frac{0 \cdot 107 \times 0 \cdot 13}{0 \cdot 0012} = 11 \cdot 5^\circ \text{ C.},$$

difference of temperature between inside and outside of coil. As a matter of fact, the temperature of the top conductor will be more than this, because it carries the heaviest eddy current.

The air-gap. The air-gap under the main poles of modern rotary converters is made quite short. It must not be so short as to cause excessive losses in the pole faces due to the open slots. If it is made about half the width of a slot, and if the pole is built up of laminations, it will be quite long enough. A length of 0·5 cm. is enough for converters up to 150 cms. in diameter. For very large machines the air-gap will be made a little greater for mechanical reasons. On 25-cycle converters the pole pitch is usually greater and the ampere-turns per pole greater, so that one usually has a rather bigger air-gap even up to 1 cm. for large machines.

Magnetization curve. The method of working out the number of ampere-turns per pole for three different voltages, 500, 550, and 580, is shown on the calculation sheet, page 570. In plotting these on a curve, it is best to take as ordinates the flux-density in the gap rather than the voltage, so that the curve will be conveniently available for all machines built on the same carcass, whatever the voltage.

Shunt winding. The method of working out the shunt winding is exactly similar to the method described on page 331. The ampere-turns at full load have been taken at 6330 instead of 4230 to enable the poles to be over excited by 2100 ampere-turns. This is to make the rotary converter draw a leading current. The effective armature-turns per pole when the converter is drawing full-load current wattless are 7000 (see page 599), so to draw 0·3 of full-load current we will require $7000 \times 0 \cdot 3 = 2100$. The cooling conditions on the shunt coil are worked out as shown on page 231. The allowance 18·7 sq. cms. per watt is very liberal for a 50-cycle converter, because as a rule there is a very great draught from the commutator necks, which is diverted in a horizontal direction, and gives very good cooling conditions.

Series winding. This is not strictly necessary on this machine, because a booster is to be fitted for raising the voltage at full load. It will, however, be found that the addition of a series winding greatly facilitates the work of the booster, and enables a smaller machine to do the work. As the booster brings up the voltage, the excitation of the converter ought to be automatically increased, so as to be equal to the excitation corresponding to the voltage in question, as ascertained from the magnetization curve of the machine. If there is no series winding, the excitation will not be sufficiently increased at the higher voltages, and the power factor will change towards the lagging side, so that the booster will have to be more highly excited to bring up the voltage.

It is not necessary to have more than one turn on the series winding, and even this will be shunted so that it will not carry the full-load current.

Commutating pole winding. The calculation of the commutating pole winding will be understood from the formulae given on page 480, and from the dimensions

of slots and pole given on the calculation sheet. In this case we have a strap coil on the armature with a short throw.

$$L_n = 1{\cdot}25\left(\frac{1{\cdot}8}{3\times 1{\cdot}1}+\frac{0{\cdot}7}{1{\cdot}1}\right)=1{\cdot}48,$$

$$L_c = 1{\cdot}25\times\frac{2{\cdot}25}{2\times 1{\cdot}4}\times\frac{17{\cdot}5}{31}=0{\cdot}57,$$

$$L_s = \frac{47}{31}\times 0{\cdot}46\left(\log_{10}\frac{47}{1{\cdot}6}-0{\cdot}2\right)=0{\cdot}85,$$

$$L_n + L_c + L_s = 2{\cdot}9,$$

$$\mathrm{B}_c = 2{\cdot}8\times 2{\cdot}9\times\frac{1340}{4{\cdot}49}=2430,$$

$$p_s + b_b - c_b = 4{\cdot}49.$$

As the commutating pole has not the full axial length, but only 15 cms., or 17·5 cms., allowing for fringing,

$$\frac{31}{17{\cdot}5}\times 2430 = 4300 = \mathrm{B}_c'.$$

If the length of air-gap under the pole is 1·37 cms., we have

$$0{\cdot}796\times 4300\times 1{\cdot}37 = 4700 \text{ ampere-turns per pole};$$

so that two turns, each carrying 2360 amperes, will be sufficient.

Commutator and brush gear. One of the difficulties in the past with 50-cycle converters has been to get a great distance between the positive and negative brush arms. The time taken for a commutator bar to pass over the pitch of the brushes is only $\frac{1}{100}$th second, so that if we make the pitch 25 cms. we get a circumferential speed of 25 metres per second (about 5000 feet per minute). This speed is found to give satisfactory operation. If we make the measurement of the brush holder on a circumferential direction rather small, we can get a clear 22 cms. between brush holders, a distance quite great enough to cause the brush arms to clear themselves if a flash-over should accidentally occur. A type of brush holder which is good for this purpose is that illustrated in Fig. 515.

The diameter of the commutator in this case will be 112 cms. to give us 14 × 25 cms. of circumference. There will be 672 bars, giving us 11·5 mean volts per bar, and 11·5 ÷ 0·72 = 16 volts actual. The length of the commutator depends upon the number and size of brushes.

Width of brushes. From one point of view, when commutating poles are used, there is an advantage in a wide brush, because it lengthens the time of commutation and lowers the voltage required to reverse the current. The brush, however, must not be so wide that the arc moved through by the short-circuited coil extends under the horns of the main poles. In order to make sure of the position of the coil under commutation, with respect to the main poles and the commutating pole at various stages of the motion under the brush, it is well to make a paper model of the bars and coils and rotate them on a drawing of the brushes and poles. If this is done with the machine under consideration, it will be seen that it is not wise to make the brushes much wider than 2 cms., or the short-circuited coil will not be sufficiently under the control of the inter-pole, but will sometimes be moving in a field of the wrong value. This circumstance limits the width of the brush.

FIG. 515.—Longitudinal section of 1250 K.W. rotary converter designed to comply with Specification No. 14, page 570.

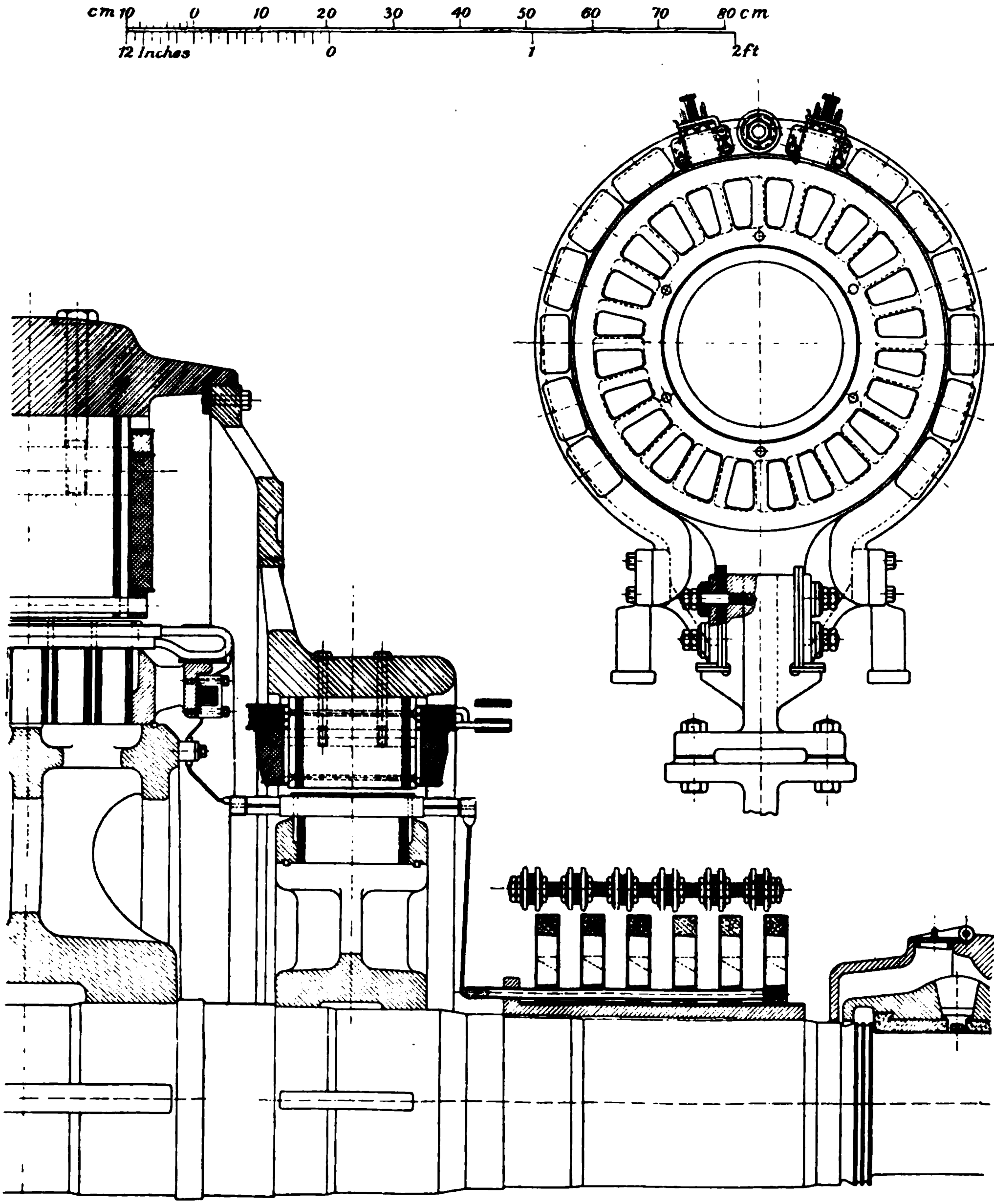


FIG. 516.—Longitudinal section of rotary converter and A.C. booster, mounted on the shaft between the converter armature and the slip-rings.

Brush contact area. While it is recognized that some kinds of carbon brushes work well up to densities as high as 10 amperes per sq. cm. (65 amperes per sq. in.), experience shows that machines with ample brush capacity give the least trouble. There appears to be very little to be gained in making the density less than 6 amperes per sq. cm., and that may well be taken as a standard for traction rotaries where we do not wish to cut down the cost to the smallest possible amount.

If we put six brushes per arm, each with a contact area $2 \times 4{\cdot}5$ cms., to collect the 336 amperes per brush arm, we get a density of 6·2 amperes per sq. cm.—quite a suitable figure. At 50 per cent. over load we will have a little over 9 amperes per sq. cm.

Grade of brush. For a high-speed commutator it is desirable to choose a brush with a great deal of graphite in its constitution. The brush, however, should not be so friable as to wear badly in the holder. It should be fitted with flexible pig-tails of low resistance, capable of taking heavy over-loads without overheating. Many of the graphitic brushes of sufficiently solid composition give a fairly low contact voltage drop, and we may allow 1·7 volts for the drop in positive and negative brushes taken together. If we use a good metal-carbon brush on the slip-rings, taking care to have no chattering, and to keep the brush well in contact with the ring, we can get the contact drop per pair of rings as low as 0·7 volt, equivalent to 1 volt C.C. This enables us to estimate the total brush loss by multiplying the continuous current by 2·7. In practice, however, it will often be found that the total brush drop over positive and negative brushes is as high as 2·5 volts, and if the slip-rings are not working well we may have an additional volt lost there.

Efficiency. According to the specification, the machine will be judged by its calculated efficiency and not from its efficiency measured from input and output. We may take the losses as given on the calculation sheet. The figure 21 K.W. for friction and windage includes 4 K.W. for brush friction. The iron loss with ordinary good dynamo sheet steel ought not to exceed the values calculated on the sheet, because Fig. 29 gives the losses rather on the high side. In taking the field loss, we should take the shunt excitation rather less than 6330 ampere-turns per pole, because the series turns contribute a substantial amount. We may take the shunt excitation at 4000 for 525 volts. That will give us 6·15 amperes at 525 volts, or say 3·3 K.W. loss in shunt and rheostat. We multiply the armature resistance by 0·378, and to this add the resistance of the commutating poles and the diverted series winding. This combined resistance multiplied by 2360^2 gives us 11·7 K.W. loss at full load, and the other figures set out on the calculation sheet at the other loads. The brush losses may be conveniently obtained by multiplying the armature current by 2·7.

Design of the amortisseur. It appears from the Specification No. 14 that the A.C. power is supplied by steam turbines. As the speed of the steam turbine is very uniform, we will not be troubled with an unsteady frequency, so that the dampers may be of the very simple character shown in Fig. 511. They are built up of three round rods passing through holes near the pole face, and two shaped bars flanking the poles, the whole connected by two stout copper bars. In this case the dampers are not connected from pole to pole, as this is not necessary. If the source of the A.C. supply had been such as to give us an unsteady frequency,

more elaborate precautions would be taken to obviate hunting. The matter is considered further on page 601.

Direct-connected exciter. This converter is intended to run at certain times from the C.C. side as a motor and supply alternating current from the transformer to the high-tension mains. If it is intended that it shall always be in parallel with a synchronous generator, when it is running in this way, no exciter for the converter will be necessary. But if it is intended that a converter or converters shall be the only machines supplying the power to the A.C. system, then it will be necessary to excite each converter by means of an exciter driven by itself for the purpose of keeping the frequency nearly constant. If no such exciter or equivalent device were employed, there would be nothing to maintain the speed of the converter. A heavy lagging load upon the A.C. side would weaken the field-magnet of the converter, and the speed would rise. With rising speed the inductive load would call for more lagging current, and the converter might run away. If a direct-driven exciter, with an unsaturated field-magnet, is used to excite the converter, a slight change in the speed of the set raises the exciting voltage by a large percentage, and so corrects the tendency for the speed to go up. One should build the exciter with the saturation of the field-magnet at its working voltage well below the knee of the magnetization curve. Such a machine, when increased in speed by 1 per cent., will give a rise in voltage of 5 per cent. or more.

THE DESIGN OF AN A.C. BOOSTER.

The driving of the booster. An A.C. booster for changing the electromotive force supplied to the taps on a rotary converter should be mounted on the shaft of the rotary, so as to ensure perfect synchronism. So long as the output of the booster is only 10 or 15 per cent. of the output of the rotary, the power required to drive it when raising the voltage can be supplied by the converter, which then runs partly as a synchronous motor. When lowering the voltage the booster runs as a motor, and the converter then acts partly as a C.C. generator. It is found in practice that the commutation of a suitably designed converter is not interfered with for small ranges of boosting, though in theory there is an unbalanced armature reaction in the armature, in so far as it acts as a motor or generator. When the booster is raising the voltage the converter will be acting as a motor to a certain extent, and this will give an armature reaction which will assist the commutation. When the booster is lowering the voltage the rotary acts as a generator, and the armature reaction opposes the commutation, but the resistance of the brushes is sufficient to prevent either of these effects becoming apparent where the output of the booster is small as compared with that of the rotary. This matter is one, however, which must be borne in mind where the percentage of voltage variation required is very great. It is, of course, possible to drive the booster by an independent synchronous motor. Where this is done great care must be taken to secure true synchronous running, or the effect on the commutation and regulation of the rotary will be disastrous. A synchronous motor for such a purpose should be designed with a very strong field, and provided with a very heavy damper. Such an arrangement is not to be recommended unless the frequency of supply is very steady, because

any phase-swinging which might occur on the rotary and booster would at times get out of step, and cause bad fluctuations in the rotary voltage.

The usual practice is to drive the booster by mounting it on the shaft of the converter, either between the slip-rings and the converter's armature (in which case one must have a rotating-armature booster), or outside the slip-rings (in which case it is best to have a rotating field). The advantage of the first arrangement is that it makes a very compact machine with no extra terminals, except the field-terminals of the booster. The outer ends of the booster armature winding are connected directly to the slip-rings, and the inner ends directly to the taps on the rotary armature (see Fig. 505). The field-frame of the booster can be conveniently mounted on the yoke of the converter (see Fig. 516). This arrangement is to be recommended in all cases where the booster is required to be continually in circuit. There are, however, some cases where the booster is only required occasionally, and where it is desirable to cut it out of circuit at times when it is not wanted. In these cases it is more convenient to mount the booster outside the bearings of the converter. The rotating field of the booster can generally be over-hung. If the booster is wound for six phases, six terminals would be required to take the current into the stationary armature, and six more to take the current out. It is therefore desirable to arrange for a stationary armature booster to be connected in three phases only. This will be possible on a six-phase rotary, if we do not inter-connect the middle points of the transformers so as to make a six-phase star.

Size of frame. The case of the over-hung booster will, in practice, be found to be the exceptional case. Most boosters will be built with rotating armatures placed between the slip-rings and the converter armature. We will choose this type of machine for the booster, which we will design to meet Specification No. 14. It will be found convenient, and, on the whole, more economical, to develop a frame of a certain diameter to be used as a booster in connection with a certain diameter of rotary. A common output for a booster is 10 per cent. of the output of the converter, so we should choose a diameter of the booster frame which will make an economical machine for that output. Smaller outputs will then be put on the same diameter with the frame shortened, and larger outputs on the same diameter with the frame lengthened. This plan, though calling for more material than would be necessary with the theoretically best diameter, will be found to work out, on the whole, most economically when the development expenses are taken into account. Moreover, it is not worth while to cut down the material to the smallest possible amount, because in any case the cost of the booster, though high in comparison with its output, is a small percentage of the total cost, and a little more or less material hardly affects it; whereas if we put in a good large section of copper in the armature we reap the benefit in the increased efficiency. If we were to cut down the armature copper to the smallest amount that would meet the temperature guarantees, we would make the I^2R losses in the booster nearly equal to the I^2R losses in the rotary itself.

In the case under consideration, the machine is required to bring down the voltage to 460 and to take it up to 550. For traction work it is to compound from 525 to 550 volts. In the compounding it will be assisted by the series winding, so that if we aim at 45 volts C.C., or 28 volts three-phase, it will be sufficient. That

is to say, the output of the booster will be about 9 per cent. of the output of the converter.

For a six-phase converter with a revolving armature booster we will usually have a six-phase armature (if placed between the slip-rings and converter armature), there being as many circuits through the booster as there are taps on the converter. The scheme of connections is that shown in Fig. 505. For a multipolar machine there will be as many paths in parallel in each of the six phases as there are pairs of poles. Thus, in the case under consideration, there will be $6 \times 7 = 42$ paths through the booster. This makes the determination of the constant K_e a little perplexing, unless we adopt a rule which takes us from the six-phase case to the three-phase case. We may argue as follows : Consider how many conductors would be required on an ordinary three-phase, star-connected armature, and take this number as the Z_a in the formula (1) given on page 24. These conductors will form three of the legs under two poles in Fig. 505. We will ultimately have to find room on the armature for the other three legs, but they will not add to the generated E.M.F. If we have more than one pair of poles, we will have as many paths in parallel per phase as there are pairs of poles.

In the example under consideration we want 45 volts C.C. or 28 volts three-phase. Now, the field being stationary, we can, in general, have a rather wider pole face than we would have on a revolving field, so we will take the constant K_e at 0·41 (see page 30).

The calculation sheet is given on page 582 and a drawing of the booster on Figs. 512, 514 and 516.

As the method of working through the calculation sheet is so similar to the method described in connection with the A.C. generators described on pages 321 and 348, it is not necessary to go through it in detail. We will just refer to those points which are special to this machine.

We have chosen 168 slots or 12 per pole, and we have 2 conductors per slot. This would give us 48 conductors per pair of poles, or 8 conductors per phase for a six-phase machine. It is, however, convenient to have an odd number of conductors per pole, because we wish one terminal of a coil to be on the outer end of the armature for connection to a slip-ring, and the other terminal to be on the inner end for connection to the converter armature. We therefore choose 7 conductors per phase, and leave out one of the 8, a piece of treated wood taking the place of the 8th conductor in the slots. We thus have 7 conductors in each branch of the star ; that is to say, 21 conductors on a three-phase machine. Our voltage formula then becomes

$$28 = 0{\cdot}41 \times 7{\cdot}15 \times 21 \times A_g\mathsf{B} \times 10^{-8},$$
$$A_g\mathsf{B} = 0{\cdot}455 \times 10^{8}.$$

Although we only count 21 conductors for the purpose of this formula, we must provide room for another 21, which form, as it were, another three-phase machine with the phases displaced by 180 degrees from the first set of conductors.

It will be seen that on the size of frame taken the magnetic loading and the current loading are both quite light. With only 17,700 C.G.S. lines in the teeth, and the current loading only 170 amperes per cm. of periphery, it is not necessary to work out the cooling conditions in the armature.

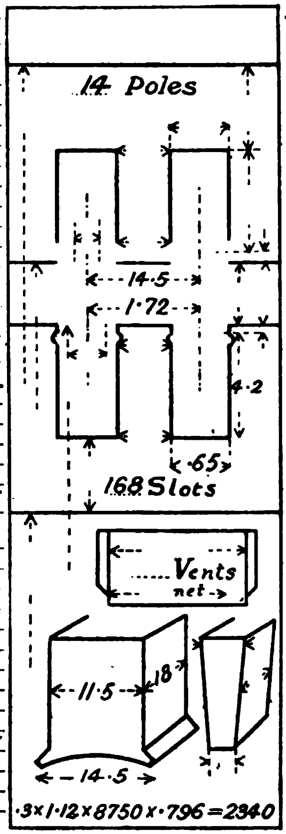
Date 9th June 1913. Type A.C. Booster GEN. ~~SYN MOTOR ROTARY~~ 14 Poles: ...... Elec. Spec. 14a
K.V.A. 115 ; P.F. ......; Phase 6 ; Volts 28 3ph. ; Amps per ter 1160 ; Cycles 50 ; R.P.M. 428 ; Rotor Amps ......;
H.P. .... Amps p. cond. 166 Amps p. br. arm. .... Temp. rise 40°C ..... Regulation .... Overload 25% 3hrs.

Customer ....; Order No. ....; Quot. No. ....; Perf. Spec. ....; Fly-wheel effect ....

Frame 92/18 Circum. 289 ; Gap Area 4760 ; poss. $A_g B$ ....; poss. $I_a Z_a$ ....; $\frac{I_a Z_a}{\text{Circum.}}$ 170 ; $\frac{D^2 L \times RPM}{K.V.A.} = 5{\cdot}7 \times 10^5$;
Air .... $A_g B$ $\cdot 455 \times 10^8$; $I_a Z_a$ 49,000

K_e ·41 ; 28 Volts = ·41 × 7·15 × 21 × ·455 ; Arm. A.T. p. pole 3100 .... Max. Fld. A.T. ....

Armature. Rev. ~~Stat.~~			
Core.	Dia. Outs.	92	
	Dia. Ins.	54	
	Gross Length	18	
	Air Vents		
	Opening Min. ___ Mean		
	Air Velocity		
	Net Length 18 × ·89	16	
	Depth b. Slots	13·5	
	Section 216 Vol.	48,000	
	Flux Density	11,000	
	Loss ·06 p. cu. cm Total	2900	
	Buried Cu. Total	4500	
	Gap Area ___; Wts		
	Vent Area ___; Wts		
	Outs. Area ___; Wts		
Teeth.	No of Segs 1 Mn. Circ.	270	
	No of Slots 168 × ·65 =	82	
	K_e	188	
	Section Teeth	2580	
	Volume Teeth	10,900	
	Flux Density	17,700	
	Loss ·145 p. cu. cm Total	1600	
	Weight of Iron		
Conductors.	Star or Mesh ___ Throw		
	Cond. p Slot	2	
	Total Conds	294	
	Size of Cond. ·3 × 1·5	·43	
	Amp. p. sq. cm	387	
	Length in Slots 18		
	Length outside 30 Sum	48	
	Total Length	141	
	Wt. of 1,000 380 Total	54	
	Res. p. 1,000 ·4 Total	·057/42	·00136 per leg
	Watts p.		
	Surface p.		
	Watts p. Sq.		



Field Stat ~~or Rotor~~.		
Dia. Bore	92·6	
½ Total Air Gap	·3	
Gap Co-eff. K_g	1·12	
Pole Pitch 20·7 Pole Arc	14·5	
K_f		
Flux per Pole $2{\cdot}38 \times 10^6$		
Leakage n.l. ___ f.l. ·5	$2{\cdot}88 \times 10^6$	
Area 205 Flux density	14000	
Unbalanced Pull		
No. of Seg. ___ Mn. Circ.		
No. of Slots ___ × =		
Vents		
K_s ___ Section		
Weight of Iron		

	Shunt.	Series.	Comm.
A.T. p Pole n. Load			
A.T. p. Pole f. Load	3384	2800	
Surface	31,000	19,600	
Surface p. Watt	16	16	
I^2 R.	1940	1220	
I. R.	450	·65	
Amps.	4·25	935 × 2	
No. of Turns	800	3½	
Mean l. Turn	70	70	
Total Length	7900	29m	
Resistance	106 hot	·00035	
Res. per 1,000	112	·05	
Size of Cond.	·015 sq. cm	4 sq. cms.	
Conds. per Slot			
Total			
Length			
Wt. per 1,000	13·5	3600	
Total Wt.	105	104	
Watts per Sq.			
Star or Mesh			
Paths in parallel			

Magnetization Curve.			25 Volts.			28 Volts.			30 Volts.		
	Section.	Length	B.	A.T. p. cm	A.T.	B.	A.T. p. cm	A.T.	B.	A.T. p. cm	A.T.
Core											
Stator Teeth											
Rotor Teeth	2580	4·2	15,800	30	126	17,700	90	380	19,000	180	750
Gap	5200	·3			2090	8750		2340	10,300		2510
Pole Body	205	16	12,500	7·5	120	14,000	9	144	15,000	21	336
Yoke	240	20	10,700	13	260	12,000	15	300	12,700	16·5	330
					2596			3164			3926

Commutator.

Dia. ___ Speed
Bars
Volts p. Bar
Brs. p. Arm
Size of Brs.
Amps p. sq.
Brush Loss
Watts p. Sq.

EFFICIENCY.	1¼ load.	Full.	¾	½	¼
Friction and W.	1·5	1·5	1·5	1·5	1·5
Iron Loss	4·5	4·5	4·5	4·5	4·5
Field Loss	1·9	1·9	1·9	1·9	1·9
Arm. &c. I^2R	2·5	1·6	·9	·4	·1
Brush Loss					
	10·4	9·5	8·8	8·3	8·0
Output		115			
Input		124·5			
Efficiency %		92·4			

Mag. Cur. Loss Cur.
Perm. Stat. Slot
„ Rot. Slot × =
„ Zig-zag
2 × × =
1·77 × =
End × × =
Amps; Tot.
τ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

Shunt winding. Some difficulty is sometimes experienced on these booster generators in finding room for both series and shunt turns. As can be seen from the figures for the magnetization curve, the shunt ampere-turns to give 28 volts at no load are 3164. At full load the teeth will require about 220 ampere-turns more, so that we have taken 3384 as the ampere-turns to be provided by the shunt at full load. Under the specification the load must be slightly leading, so that the armature reaction will assist rather than oppose the shunt ampere-turns.

We have allowed 16 sq. cms. per watt on the shunt coils. This is sufficient, in view of the very good ventilation induced by the armature of the converter. We find that 800 turns per pole, of wire having an area of 0·015 sq. cm., will be required. Thus the exciting current at full voltage will be 4·25 amperes.

Series winding. We find that we require at full load about 2800 ampere-turns per pole for the series winding; we must therefore have more than one turn. We might put two turns per pole and divert a great part of the 2360 amperes. A rather nicer arrangement is to put three turns per pole and put two paths in parallel. We can then send one half of the current one way around the frame and the other half the other way around the frame, and thus avoid magnetizing the shaft, as we would do if we passed a large current once around the frame.

The figures for the efficiency will be found on the calculation sheet.

LARGE LOW-VOLTAGE CONVERTERS.

We will now give a specification for a 2000 K.W. rotary converter intended for electrolytic work.

SPECIFICATION No. 15.

2000 K.W. ROTARY CONVERTER, 250 VOLTS, 50 CYCLES.

Extent of Work. 240. This specification provides for the manufacture, delivery on site, erection, testing, and starting to work of rotary converters and transformers, together with all accessories and details as hereinafter specified, in the sub-station of the Company hereinafter called the Purchaser, at .

General Purposes of Plant. 241. The duty of the plant will be to convert 3-phase power supplied at a voltage of 11,000 at a frequency of 50 cycles per second into continuous-current power at from 230 to 270 volts, to be used in electrolytic work. The plant must be suitable in all respects for this purpose.

Characteristics of Rotary. 242. The rotary converter shall have the following characteristics:

Normal output when running A.C. to C.C.	2000 K.V.
Number of phases	6.
Frequency	50 cycles per second.
Normal continuous-current voltage	250
Continuous-current amperes	8000.
Kind of excitation	Shunt wound.
Adjustment of voltage on rheostat	230 to 250 Tap (1). 240 to 260 Tap (2). 250 to 270 Tap (3).
Leading idle power required when running A.C. to C.C., at the highest voltage on any tapping	600 K.V.A. leading wattless.
Over-load capacity	25 per cent. for 3 hours at unity power factor. 50 per cent. for 10 minutes at unity power factor.

Temperature rise after continuous full-load runs at 250 volts, 0·955 power factor on H.T. side	40° C. by thermometer.
Temperature rise after 3 hours 25 per cent. over load	55° C.
Puncture test	23,000 volts alternating at 50 cycles applied for 1 minute between transformer high-tension windings and frame.
	1500 volts alternating at 50 cycles applied for 1 minute between all low-tension windings and frame.
Mean voltage between commutator bars not to exceed	11 volts.

243. The rotary converters are only intended to run from A.C. to C.C. A.C. to C.C.

244. The 6-phase rotary converters shall be of the two-bearing horizontal type with shunt-wound field magnets and commutating poles. Type.

245. The speed shall not exceed 250 R.P.M. Speed.

246. They shall be fitted with commutating poles. Commutating Poles.

247. Each rotary converter shall be connected by cables running directly from the slip-ring brushes to the L.T. transformer terminals without the intervention of any switch gear. Connections.

248. It shall be started by means of a starting motor direct-connected to the shaft, and shall be synchronized on the high-tension side of the transformer. Starting.

249. The efficiency of each rotary converter shall be calculated from the separate losses, which shall be measured in the following way: Efficiency.

(*a*) *Iron loss, friction and windage.* The machine shall be run as a C.C. motor at full speed and at various

voltages, and measurements shall be made of the C.C. power taken to drive it, and of the exciting current taken at various voltages.

(*b*) *Copper losses.* The resistance of the armature of the rotary and booster and of their field windings shall be taken by measuring the voltage drop in them when a substantial current is passed through them. From these resistances, after making due allowance for the observed temperature rise, the I^2R losses shall be calculated on the assumption that on a 6-phase converter the armature copper loss is 0·3 of the copper loss when loaded as a C.C. generator.

(*c*) *Brush losses.* The brush I^2R losses shall be taken as equal to the number of watts obtained by multiplying the continuous current delivered by the converter by 3 volts, unless the Contractor can demonstrate to the satisfaction of the Purchaser that on his machine the sum of the brush losses on the C.C. side and the A.C. side is substantially less than this, in which case the actual voltage drops on the commutation brush and slip ring brushes shall be taken.

(*d*) *Transformer iron losses.* These shall be taken by measuring the number of watts supplied to operate the transformer at full voltage, 50 cycles at no load.

(*e*) *Transformer copper losses.* These shall be taken to be equal to the power required to circulate full-load current through the transformer when short circuited.

Efficiency Guarantee.

250. The Contractor shall state what calculated efficiency he is prepared to guarantee on the above basis. He shall guarantee that the combined plant in commercial service shall have an overall efficiency not more than 1 per cent. lower than the calculated figure.

Number of Hours per annum.

251. The converters are intended to run in almost continuous service, and each machine will probably run at approximately full load for 7000 hours per annum.

Value of 1 per cent. saved in Efficiency.

252. The cost of electrical energy may be taken at approximately 0·25 pence per kilowatt hour. So the value of each 1 per cent. in efficiency is about £145 per annum.

Bonus and Penalty.

253. In view of the great importance of high efficiency the Purchaser will pay a bonus of £50 for each $\frac{1}{10}$th per cent.

by which the efficiency of the combined plant (transformer and converter) shall exceed the guaranteed calculated efficiency, and the Contractor shall pay a penalty of £50 for each $\frac{1}{10}$th per cent. by which the efficiency shall fall below the guaranteed calculated efficiency.

If the efficiency of the plant shall fall below 92 per cent., the Purchaser shall be at liberty to reject it.

Figures for the calculated efficiency shall be given for full load, three-quarter load and half load, both at unity power factor and at 0·95 leading power factor measured on the H.T. side, but the bonus and penalty shall only be paid in respect of the efficiency at full-load, unity power factor.

254. Tappings shall be provided on the high-tension side of the transformers, whereby the range * of voltage obtainable on the C.C. side shall be from 230 to 250, or 240 to 260, or 250 to 270. Under these conditions the rating of the plant when running on the 230 to 250 tappings shall be taken to be 1900 K.W. **Wider Range of Voltage by Means of Transformer Tappings.**

255. On the middle set of tappings the voltage shall be varied from 240 to 260 by changing the excitation of the converter, and rheostats shall be provided to enable the range specified on each set of tappings to be obtained whether the machine is hot or cold.

256. The plant shall be so designed that by means of regulating field rheostats the power factor in the H.T. side at full load can be varied from unity to 0·95 leading, the pressure on the H.T. supply being 11,000 volts and the periodicity being 50 cycles per second. When the power factor is 0·95 leading, the voltage may be above the middle voltage obtained from the tapping, but it must not be higher than the highest voltage in the range specified. **Variation of Power Factor.**

* If the converter were to be provided with a booster, the following clause would be inserted instead of 254, 255, and 256 :

256a. The plant shall be so designed that when running under working conditions, supplying current for electrolytic purposes, the continuous-current pressure shall remain steady so long as the high-tension A.C. pressure shall remain steady, and the adjustments remain undisturbed. It shall, however, be possible to obtain on the C.C. side any voltage from 228 to 275 by the adjustment of the booster and rotary converter rheostats, so long as the high-tension pressure is maintained at 11,000 volts, and the frequency at 50 cycles. It shall, moreover, be possible to maintain the power factor of 0·95 leading with any C.C. voltage between 240 and 275 volts. **Variation of Voltage.**

Changing over of Load. 257. It shall be possible by means of the rheostats provided to change the load from one rotary converter to another without alteration of the C.C. voltage of supply.

Field Regulating Rheostat. 258. Two field regulating rheostats of approved type shall be provided for each rotary converter. One of the rheostats for the rotary converters shall be placed adjacent to the high-tension panel for convenience of adjustment while the converter is being synchronized, and the other shall be placed adjacent to the C.C. panel for convenience of adjustment of the C.C. voltage. **Steps.** The steps in this latter rheostat shall be so fine that it shall be possible to obtain over the whole range variations not exceeding 1 volt per step. The rheostats shall be supplied with the necessary face-plates, bevelled gearing (if necessary), and hand-wheels.

Starting Motor. 259. Each starting motor shall be of the squirrel-cage induction type mounted on an extension of the armature shaft and suitable for running on a 3-phase, 50 cycle, 440 volt circuit. This circuit will be provided by the Purchaser, and fed from independent transformers supplied under another contract. Each starting motor shall be of ample capacity, and shall be capable of driving the rotary converter at full speed, normal excitation, for at least 20 minutes, so as to enable the high-tension side of the static transformers to be synchronized with the high-tension system, under all conditions of commercial operation. They shall be so designed that when a converter is running at synchronous speed the A.C. voltage generated by it shall not differ from the normal supply voltage by more than 15 per cent. either way, and provision shall be made so that the slip of the starting motors can be readily varied to meet this condition.

Absence of Hunting. 260. Under any of the conditions of voltage and periodicity contemplated in this specification, and under practical working conditions, the rotary converters and transformers shall run parallel with one another, with the rotary converters now existing in the sub-station, and with the high-tension system, without hunting or falling out of step. This steady operation shall be maintained notwithstanding the fact that the load may be fluctuating from no load to 50 per cent. over load, and that the total load is unequally divided between different machines in the sub-station.

261. Each rotary converter shall operate sparklessly under all normal working conditions from no load to 50 per cent. over load. It shall be possible to obtain 100 per cent. over load for 2 minutes without causing such sparking or heating as will injure the brushes or commutator. The above conditions as to operation shall be met without rocking the brushes. **Commutation.**

262. The commutator of each rotary converter shall be designed with a view to low-temperature rise and perfect commutation. It shall consist of hard-drawn copper segments insulated from each other and from the frame by means of mica. The mica between the segments shall be of such quality that it wears at the same rate as the copper. The wearing depth of the commutator on the machine put forward shall be stated; and due preference will be given, when considering tenders, to machines having great wearing depth. The proposed peripheral speed of the commutator shall be stated. No peripheral speed of the commutator is here specified, but no tender will be considered unless the tenderer can show satisfactory results obtained on similar machines with as high peripheral speed as proposed in his tender. **Commutators.**

263. Each tender shall show by means of drawings the type of C.C. and A.C. brush gear proposed and the arrangements for supporting it. All brush gear must possess the following characteristics: (*a*) The supports must be very rigid and not liable to alteration of position; (*b*) Each brush must slide in a manner truly parallel to a given direction; (*c*) It shall slide or move without more friction than is necessary to prevent chattering, and the amount of friction must be reasonably uniform; (*d*) The current shall be led out of or into each brush by means of flexible connections; (*e*) The positions of the brush holders on the brush arms shall be staggered so as to prevent uneven wear on the length of the commutator or slip ring; (*f*) The holders shall be designed so that adjustments of position and pressure are easily made when the machine is running, and so that the brushes can be easily removed and replaced; (*g*) The parts shall be so shaped that they shall not be injured by an arc if the machine flashes over. **C.C. and A.C. Brush Gear.**

264. Each rotary converter shall be provided with an apparatus for keeping the armature moving backwards and forwards axially, so as to prevent the formation of ruts on the commutator and slip rings. **Oscillator.**

Type of Brushes and Flexible Connections. 265. The type of brushes to be used on the commutator and on the slip ring, and also the type of flexible connections, shall be stated. Tenders shall also state the shortest distance from one C.C. brush arm to another.

Current Density in Brushes. 266. The current density in the brushes at full load shall be stated, and in considering tenders, preference will be given to designs having a small current density.

Terminals and Connections. 267. All connecting straps which bring the current from the brush holders to the terminals must be bolted together so as to break joint and present large conducting surfaces where the current passes from one strap to another. The terminals shall be designed to fit into the copper straps provided by the Purchaser for conducting the current from the rotary to the switchboard, particulars of which will be supplied. The design of these terminals must meet with the approval of the Purchaser. Independent terminals shall be provided for the ends of the field windings. The connections between the field coils shall be very substantial, there being no loose unanchored wires free to vibrate.

Bearings. 268. The bearings shall be of the self-lubricating type, preferably with revolving oil-rings. They shall be self-aligning and split horizontally. The bottom bearing shall be arranged so that it can be removed without raising the shaft more than 0·1 inch. It must be possible to remove either bearing cap without dismantling the brush gear. The bearing pedestal shall be provided with oil gauges and drain cocks. The oil wells shall be so covered that no dust can enter, and the caps of the oil wells must be made so that they are not detached from the housing when opened. The design of the journal oil throwers and oil catchers must be so perfect that no oil is visible outside the bearing after a six hours' run.

Insulation. 269. The insulation of all conductors lying in slots must consist largely of mica, and must be treated to prevent deterioration due to moisture.

Sample Coil. 270. A sample armature coil similar to that proposed to be used on the machine in question, showing the arrangement of straps and insulation, though not necessarily of the same size, must be submitted with the tender.

271. Arrangements shall be made so that commutator grinding gear of approved type can be readily fixed to the rotary converters. **Grinding Gear.**

272. All foundations, ducts, trenches and covers will be provided by the Purchaser. Within four weeks of the placing of the order for the machinery the Contractor shall supply to the Purchaser sufficient drawings and templates to enable the Purchaser to lay out the foundations. If sufficient information is not so supplied, any alterations or additions to the work on the foundations shall be done by the Contractor or by the Purchaser at the Contractor's cost. The Contractor shall be responsible for the levelling and grouting in of his machinery on the foundations provided. **Foundations.**

273. The Contractor may use at his own risk for the erection of the machinery the overhead travelling crane provided by the Purchaser when the same is not required for other purposes. **Use of Crane.**

(See Clauses 8, p. 271; 55-59, p. 379; 125, p. 461.) **Accessibility.**

274. All connections to existing bus-bars, machinery, etc., shall be carried out at such times as are convenient to the Purchaser. **Time for making Connections.**

275. As each part of the machinery is erected, it shall be passed by the engineer of the Purchaser or his authorized representative; but such passing shall in no way exonerate the Contractor from any responsibility under his guarantee. **Checking of Work.**

276. All the working parts of the apparatus supplied shall be made to gauge so that corresponding parts shall be interchangeable wherever possible. **Interchangeability.**

277. All screw threads shall be of Whitworth's standard. **Screw Threads.**

278. Before despatch from the manufacturer's works, the rotary converters shall have all rough places filled, and shall be painted with one coat of the best paint. After erection on site they shall be given two final coats of paint of an improved colour, and finally varnished in the best manner. **Painting.**

279. Cables or other leads connecting the transformer L.C. terminals to the slip rings will be provided by the Purchaser. The terminals on the slip-ring brush-holder supports shall be provided by the Contractor; they shall be very substantial and of an approved shape. **Cables, etc.**

Spare Parts.

280. The tender shall state what spare parts are recommended; a list of such spare parts with their prices shall be set out in a schedule.

Dates of Completion.

281. The tender shall contain a schedule giving the dates at which the various sets covered by this specification can be delivered and put into commercial service.

Drawings attached.

282. The following is a list of drawings attached to this specification :

Proposed general arrangement of sub-station, drawing No. .

Diagram of main connections, drawing No. .

Drawings and Samples required.

283. The following is a list of drawings, etc., required with the tender :

1. Outline drawings showing the apparatus to be supplied in plan and in elevation.

2. Drawings showing arrangement of C.C. brush-holder arms and commutator, and showing construction of C.C. and A.C. brush gear.

3. Drawings showing foundations with the position and size of foundation bolts. These drawings should also show where the A.C. and C.C. terminals of the rotary converter will be placed.

4. Sample armature coil.

Tests.

284. The following tests will be carried out on the rotary converter set in the presence of the Purchaser's representative :

1. Iron loss, friction and windage measurement, as detailed in Clause 249*a*.

2. Measurement of resistances of rotary converter armature and field, as detailed in Clause 249*b*.

3. Measurement of transformer iron loss as detailed in Clause 249*d*.

4. Measurement of transformer copper loss as detailed in Clause 249*e*.

5. Puncture test of 23,000 volts, alternating at 50 cycles, shall be applied between all high-tension conductors and earth for one minute.

Puncture test of 1000 volts, alternating at 50 cycles, shall be applied between all low-tension conductors and earth for one minute.

During these tests all windings and connections other than those to which the test is being applied shall be connected to the core.

6. *Temperature test.* After erection the rotary converter sets shall be run at full load in ordinary commercial service until the temperature of all parts has become substantially constant. The temperatures shall then be taken of the armature copper, armature iron, commutator, brush gear, field coils, and slip rings, by means of thermometers. The temperature of the air shall at the same time be taken within three feet of the machine in line with the shaft and at both ends of the shaft; and the mean of these two readings shall be taken as the temperature of the air.

The temperature rise of the transformer oil shall be taken as the difference between the temperature in the hottest place available, and the temperature of the air in the transformer chamber taken three feet from the ground.

7. *Efficiency.* If the efficiency calculated from the foregoing tests shall be within the guaranteed figures, the fact will be taken as *prima facie* evidence that the efficiency guarantees have been met. If, however, the Purchaser can conclusively prove, by means of properly calibrated wattmeters connected in circuit after the plant is installed, that the efficiency is 1 per cent. lower than the guaranteed figures, after making due allowance for losses in cables and connections, it will be incumbent upon the Contractor to amend the plant so as to obtain an over-all efficiency within 1 per cent. of the guaranteed figures; and if it shall be impossible to so amend the plant, he shall pay a penalty to the Purchaser of not more than £ for every 1 per cent. below the guaranteed figure. Provided always that if the efficiency shall fall below 92 per cent. the Purchaser shall be entitled to reject the plant.

285. The Contractor shall give to the Purchaser or his representative seven days' notice of any tests which he proposes to carry out in the presence of the Purchaser or his representative. Two copies of the results of all such tests shall be furnished to the Purchaser.

Notice of Tests.

DESIGN OF A 2000 K.W. ROTARY CONVERTER FOR ELECTROLYTIC WORK.

As the current per terminal on the C.C. side is 8000 amperes, one of the main considerations in settling the design of this machine is the fixing of the number of poles. The greater the number of poles, the fewer the amperes to be commutated at each brush-arm, and the slower the speed. If first cost and efficiency were of no importance, we would make a large number of poles. If we chose 32 poles we would have only 500 amperes per brush-arm, which, at a voltage of 250, would be quite easy to commutate. The length of commutator would then be some 16 inches, and the cooling conditions would be good. However, 32 poles would give us a speed of only 187 R.P.M.; the size and cost of the machine would be rather greater than really necessary. Experience shows that on the machines which are to carry a steady load at a voltage of about 250, it is possible to go to 800 amperes or more per brush-arm without endangering the commutation. It would, therefore, be quite possible to choose only 20 poles and yet make a very good machine, but the specification in this case says that the speed shall not be higher than 250 R.P.M. We must, therefore, have at least 24 poles in this case. We will have 666 amperes per brush-arm at normal load, and 835 amperes per brush-arm at 25 per cent. over load.

As it is important to make the efficiency as high as possible, we will not work the copper at a very high-current density, and we will keep down the saturation of the iron. It will, therefore, be advisable to keep the pole pitch as great as 32 cms. We will take 12 slots per pole as before, but now there will only be 4 conductors per slot, each carrying a normal current of 333 amperes. The conductor might be made 0·41 cm. × 1·4 cm., giving a nominal current density of about 600 amps. per sq. cm. These will go into a slot 1·1 cm. by 4·1 cms., and we will find that with a diameter of armature of 245 cms. and a length of 30 cms., after due allowance for ventilating ducts and insulation, we can get a total cross-section of all the teeth of 10,300 sq. cms.

The magnetic loading will be found by the formula

$$258 = \cdot 74 \times 4\cdot 16 \times 48 \times A_g\mathrm{B},$$
$$A_g\mathrm{B} = 1\cdot 75 \times 10^8.$$

Dividing by 10,300 sq. cms. we get a maximum density in the teeth, $\mathrm{B} = 17{,}000$. This is a suitable figure for a machine of this character, so we may adopt the dimensions given above.

It is unnecessary to go through all the steps in the calculation of the machine, as these are very similar to those described on pages 321 and 567.

The winding of the commutating pole may consist of one turn through which 4000 amperes pass, there being two paths in parallel through the commutating winding. In one of these halves the current will pass one way around the shaft, and in the other the other way, to avoid magnetizing the shaft.

There will be no series winding.

Sometimes, for electrolytic purposes, it is desirable to change the voltage of the continuous-current supply. If this must be done over a wide range and in a continuous manner, that is to say, by going from one voltage to another in infinitely

small steps, it is best to supply an A.C. booster for the purpose. In this cases however, as will be seen from Specification No. 15, it is sufficient to give a continuous range of voltage over a small range (240 to 260 volts), and at times when a lower or higher voltage is needed, the Purchaser is content to make the change by changing the tapping on the transformer. This is a much more economical method than the one requiring a booster, the copper losses in which would always be going on whether the change in voltage were required or not. The range from 240 to 260 volts can be obtained very readily by changing the excitation and causing a reactive drop or a reactive rise in the transformer. The theory of this method is given below. As we only require about 4 per cent. change in voltage from the mean in this case, it is sufficient to give the transformer a 10 per cent. reactive drop between no load and full load ($\cos \phi = 0$).

THE VARIATION OF THE VOLTAGE OF A ROTARY CONVERTER BY THE VARIATION OF ITS EXCITATION.

A transformer having considerable magnetic leakage behaves in some respects like a choke coil. When it is fed with a constant alternating voltage on the primary side, the voltage at the terminals of the secondary for a given load will depend upon the power factor of the load. If the current lags, there will be an inductive drop in the transformer; whereas if the current leads there will be an inductive rise. One of the simplest ways of representing the relation between the primary and secondary voltage of a transformer for various loads and power factors is that given in Fig. 517. For the purpose of this figure, the phase of the secondary voltage is represented by a vertical line, and is taken as the standard of reference for the phase of all other quantities. The primary voltage will change its phase with respect to this datum line, but the vector OE_1 representing it will always be of the same length, provided the primary voltage remains constant. We may therefore draw the arc AE_1B of a circle with its centre at O, to give us the locus of the point E_1. The inductive drop which occurs in the transformer is approximately at right angles in phase to the secondary current, and the amount of it depends upon the design of the transformer. For the purpose of what follows here, we shall assume that a transformer can be built so as to give

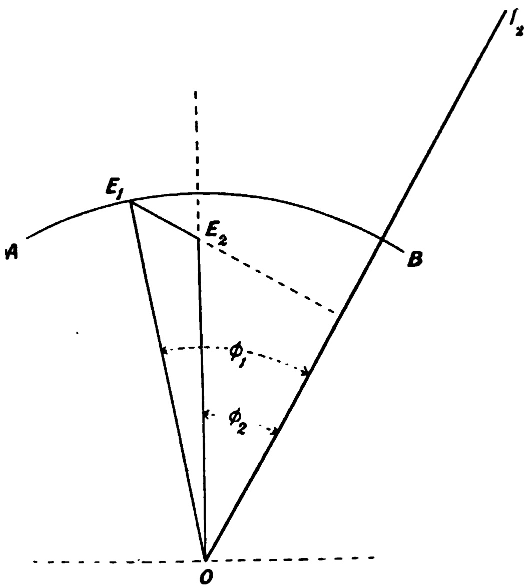


FIG. 517.—Phase relations between primary OE_1 and secondary OE_2 voltages of a transformer with current OI_2 lagging.

any required inductive drop when carrying its full-load current at zero power factor. Suppose, for the purpose of illustration, that a transformer is designed to give at full-load current a reactance voltage equal to 20 per cent. of the normal voltage. If, then, we have a full-load current lagging 30°, as shown in Fig. 517, the line E_1E_2 will represent the inductive drop in the transformer, its length being 20 per cent. of OE_1, and its phase position being at right angles to the vector OI_2, which represents full-load current in the transformer. Neglecting the resistance drop, the line OE_2 is proportional to the secondary voltage, and will, with a lagging current, be less than OE_1. If, now, the current leads on the secondary voltage, as indicated in Fig. 518, E_1E_2, which must still be drawn at right angles to OI_2, is so placed that OE_2 is greater than OE_1.

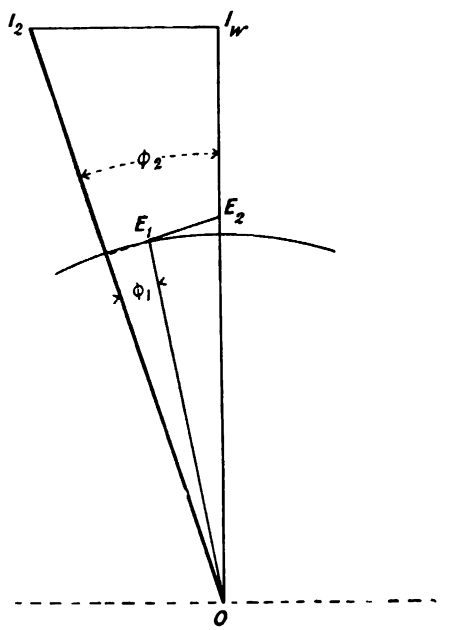


FIG. 518.—Phase relations between primary and secondary voltages of a transformer when carrying full-load current OI_2, leading by the angle ϕ_2 on the secondary voltage.

We see from Fig. 518 that the amount by which OE_2 exceeds OE_1 depends upon three factors: (1) The amount of inductance in the transformer; (2) the value of the secondary current; and (3) the value of the angle ϕ_2. If we confine ourselves to the case where full-load current is passing in the secondary, we have to consider only factors (1) and (3). We can obtain a given inductive rise of, say, 10 per cent., either by a great inductance and a small angle ϕ_2, or by having a smaller inductance and a greater ϕ_2. The amount of inductance which we should give to a transformer in any rotary converter installation will depend upon certain circumstances which we shall consider presently (see page 599). We will assume for the moment that we have decided upon the amount of inductance: this is usually measured by the percentage reactive drop obtained in the transformer when carrying full-load current at zero power factor.

When the load on the secondary side of the transformer consists of a rotary converter or synchronous motor, the current can be made to lead by over-exciting the converter or synchronous motor, and can be made to lag by under-exciting it. In the case of a rotary converter, an adjustment of the voltage may be made by hand by adjusting the rheostat in circuit with the shunt coils. Where the converter is to be over-compounded, the change in the number of ampere-turns per pole is effected automatically by the series windings. The shunt excitation is adjusted so that at no load a lagging current is drawn from the transformer; then, as the load increases the excitation becomes normal, and with a further increase in the load the converter becomes over-excited and draws a leading current.

The method of working out the number of ampere-turns which must be added to the normal excitation of a rotary, in order to obtain a given rise in voltage, will be best understood from an example worked out.

Consider a 1250-K.W., 550-volt, six-phase rotary converter, and suppose that it is desired to make it compound from 500 volts, no load, to 550 volts, full load. In order to proceed, it is necessary to have the following data:

1. The number of conductors in the armature and the current per conductor at full load.
2. The percentage reactive drop in the transformer at full load.

We will assume that the machine is a 14-pole machine, like that particulars of which are given on page 582, having 12 slots per pole and 8 conductors per slot. We will further assume that the reactive drop in the transformer is 20 per cent. of the normal voltage when full-load current at zero power factor is drawn from the secondary. The amount of this reactive drop may be fixed in order to suit the circumstances of the case, as explained on page 599.

It is best to draw the graphic diagram as if the transformer ratio were 1 : 1. It is well to allow about 3 per cent. (in this case 16 volts) for ohmic drop in transformer, armature, brushes and field windings. Thus it is necessary to generate 566 volts at full load. Now, if the excitation of the rotary were adjusted so as to give us unity power factor at no load, it would be necessary to over-excite the field-magnet and make it draw a leading current sufficiently great at full load to give a rise of 66 volts. This would cause much more heating in the armature than if we adopted the plan of arranging the excitation so that at about half load the converter is running at unity power factor and generating, say, 533 volts. It would then only be necessary to over-excite it so as to obtain an additional 33 volts at full load, and this would be done with a smaller leading component than if the whole 66 volts had to be obtained by over-excitation. The drop in voltage of 33 volts between half load and no load can easily be obtained by drawing a lagging current from the transformer at no load. This is the plan usually adopted, though it is not important that the excitation which shall give unity power factor shall occur exactly at half load.

Let us decide, then, in the first instance, to run at about unity power factor at 533 volts. This will fix the ratio of transformation of the transformer, which will be so adjusted that on unity power factor we have 373 volts between rings 1 and 4 of the converter. Taking the ratio of transformation of 1 : 1 for the purpose of our diagram, we draw the arc of the circle at a radius, OE_1, of 373. At full load it is desired to generate 565 volts C.C., that is, 394 volts A.C. between rings 1 and 4. We therefore set off $OE_2=394$. The reactive drop in the transformer at full load being 20 per cent. of 373, we know that the radius E_2E_1 is equal to 74·6; and this can therefore be set off, taking E_2 as the centre. We thus obtain the position of E_1, which must lie on the arc of the circle. We now know that the secondary current OI_2 is at right angles to E_2E_1; its phase position is therefore ascertained. The full-load working current OI_w in phase with the secondary voltage being known (in this case 1180 amperes per phase), we can at once set off I_wI_2, the leading wattless current at full load (in this case 388 amperes), and

obtain the length of OI_2 (1220 amperes), the full-load current for which the transformer must be designed. We can now proceed to find by how much the field-winding of the rotary must be over-excited in order to cause the leading wattless current I_2I_w to flow. This depends upon the number of conductors in the armature.

If each pole of a rotary converter of normal pole pitch is over-excited by an amount equal to 0·87 of the normal C.C. armature ampere-turns, a leading wattless current of full-load value will flow in the armature. In this case the continuous current per conductor will be 168 amperes, and there being 48 turns per pole, the continuous-current ampere-turns per pole will be $168 \times 48 = 8064$.

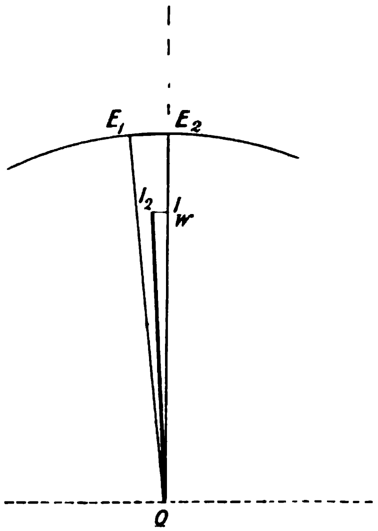


FIG. 519.—Phase relations at half load.

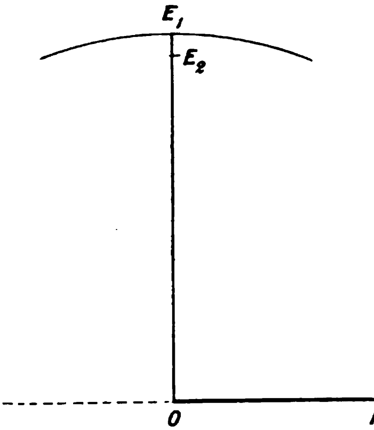


FIG. 520.—Phase relations at no load.

From Fig. 518 we see that I_wI_2 is equal to 0·33 of OI_w, so that the amount of over-excitation required to make I_wI_2 flow will be equal to $0{\cdot}33 \times 0{\cdot}87 \times 8064 = 2340$ ampere-turns.

Before we can settle the right number of series turns to put on the poles of the rotary, we must consider the conditions of running at half load and no load. Fig. 519 shows the clock diagram at half load. OI_w is now half the value it had in Fig. 518. The excitation of the pole is now nearly normal, so that OI_2 is almost in phase with OE_2. The reactive voltage in the transformer E_2E_1 is now only 36 volts, on account of its phase position $OE_1 = OE_2$, so that the rotary has a generated voltage of 533 and a terminal voltage of 525.

The clock diagram at no load is given on Fig. 520. Here the reactive voltage E_2E_1 is in phase with OE_2, so we can calculate how much it should be in order to get the desired voltage drop at no load. To get 33 volts C.C. we require 23·3 volts A.C. Now, if 1220 amperes give a reactive voltage of 74·5, it will take 383 amperes lagging current to give a drop of 23·3 volts. At no load, therefore, OI_2 must be a lagging current of 383 amperes per phase. The amount by which the excitation must be below normal to cause this lagging current to flow is calculated as follows.

Full-load current wattless ($=1180$ amperes) requires $8064 \times 0{\cdot}87 = 7000$ ampere-turns per pole; therefore

$$7000 \times \frac{383}{1180} = 2280 \text{ ampere-turns}$$

below normal excitation are required to make 383 amperes lagging current flow.

Suppose that the normal excitation for 500 volts is 3643. Then the shunt excitation would be adjusted to $3643 - 2280 = 1363$ ampere-turns per pole at no load. At 550 volts this would increase to 1500. If, now, the proper excitation for 550 volts unity power factor is 4500, we must have $3000 + 2340 = 5340$ series ampere-turns in order to draw 388 leading amperes through the transformer. As the full-load current is 2360, we require $5340 \div 2360$ or about 2·5 turns per pole in the series winding. In the above we have assumed that the reactance voltage follows a straight line law; in other words, that it is proportional to the load on the secondary. This is not always true, particularly in transformers containing "reactive" iron. When we cannot assume true proportionality, a curve showing the reactive voltage at different loads should be obtained from the designer of the transformer, and this can then be worked to in setting out the reactive voltage E_2E_1 in the graphic construction. In actual practice, in the case considered, one would put three series turns per pole on the 1250 rotary considered above in order to be on the safe side. It is then an easy matter to divert some of the series winding to obtain the amount of compounding that we require.

At half load we would have $5340 \div 2 = 2670$ series ampere-turns and 1430 shunt ampere-turns. As the total 4100 is just a little more than the normal excitation at 525 volts, the current would lead just a very little and give a clock diagram as shown in Fig. 519.

Power factor on the H.T. side and L.T. side. It will be seen from Figs. 517 and 518 that where the transformer has considerable reactance the angle ϕ_2 between the current and the voltage OE_2 may be either greater or less than the angle ϕ_1 between the current and OE_1. When the current lags ϕ_2 is less, and when it leads ϕ_2 is greater than ϕ_1. Now, the amount of heating of the copper on the armature (see p. 542) depends upon the value of ϕ_2. Where the transformer reactance is very great, ϕ_1 may be nearly zero at full load, though ϕ_2 has a high value. Thus the power factor on the H.T. side is nearly unity, and yet there may be such a low power factor on the L.T. side that considerable heating occurs in the armature copper.

In cases where it is desired to run the rotary plant on a leading power factor (that is to say, leading on the H.T. side), it is desirable to keep the reactance voltage of the transformer fairly low; and if a wide range of compounding is required, it is best to carry it out by means of a booster, so that the rotary has only to supply the leading current required on the H.T. side, and not a heavy additional leading current to compensate for the reactive drop in the transformers.

SPECIAL PRECAUTIONS NECESSARY WHEN THE FREQUENCY IS UNSTEADY.

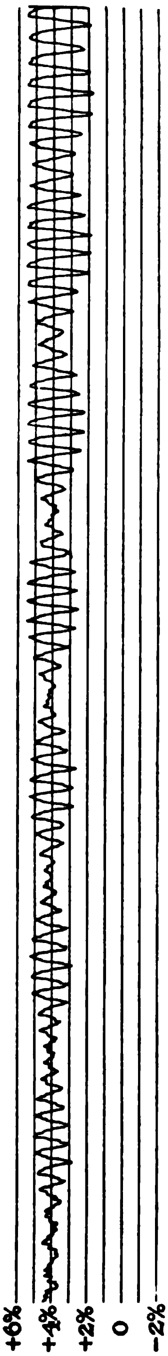


FIG. 521.—Tachograph record taken from the shaft of a rotary converter having no effective damper and running on a circuit whose frequency was unsteady. Time scale, 1 cm. = 3·2 seconds.

On pages 337 to 356 we considered the precautions which should be taken to ensure the good parallel running of synchronous machines. The same rules apply to the case of rotary converters, but here the disturbance is not so often in the converter itself as in the prime mover supplying the alternating current. In all cases where there is reason to believe that the frequency of the system is not constant, particulars should be obtained of the nature of the disturbance, and care should be taken to see that the natural period of phase-swinging of the converter does not correspond with the period of the disturbance; otherwise resonance may be set up, which, if it does not prevent parallel running altogether, may seriously affect the commutation. In one case within the experience of the author, a rotary converter would not run in parallel with slow-speed engine sets because its natural period of phase-swing coincided almost exactly with the period of the disturbance. A tachograph record of the speed of the converter was taken during the interval of time between the instant of switching the converter to the bus-bars and the instant at which it broke step. Fig. 521 gives the record. It will be seen that the speed was fairly constant during the first second or two. Then the phase-swinging was augmented and then diminished as the natural swing of the machine got into and out of step with the disturbance. A change in the excitation was sufficient to give almost perfect resonance, and the converter then broke step. The addition of considerable self-induction in circuit with the slip-rings was found to so alter the value of I_u (page 339) that parallel running became possible without any change being made on the dampers. On the other hand, if very heavy dampers had been added to the pole shoes the machine would have run even under conditions of perfect resonance.

For the satisfactory operation of a rotary converter running on a circuit with an unsteady frequency, it is not sufficient merely to reduce the phase-swinging to a point that makes parallel running possible. It is necessary to reduce it so that it no longer interferes with the commutation.

From what was said on page 338, it will be understood that when a converter is phase-swinging, it carries a motor load when the armature is being accelerated, and it carries a generator load when the armature is being decelerated. These motor and generator loads, being uncompensated, produced a field distortion that may very seriously interfere with the

excitation of the commutating pole. The designer should be able in any given case to work out roughly the amount of distortion produced.

A case is worked out below numerically. The actual amount of the phase-swing depends upon the effectiveness of the damper. On page 352 we saw that the effectiveness of a damper can be conveniently expressed in terms of the slip which the machine would have if run as an induction motor with the damper acting as a squirrel cage. A case was worked out showing how the effectiveness of a damper can be roughly estimated. We shall employ the same notation as on pages 339 and 354. By the symbol s we denote the slip there would be on the machine if run at full load as an induction motor using the damper as a squirrel-cage winding. Thus, if the slip would be 2 per cent., $s = 0{\cdot}02$. Then $2\pi ns$ is the angular velocity of the slip in a two-pole machine, and $\frac{2\pi ns}{p}$ is the angular velocity of the slip on a machine having p pairs of poles. Now, if an angular velocity of $\frac{2\pi ns}{p}$ gives the full-load torque,

$$\frac{EI}{9{\cdot}81 \times R_{ps} \times 2\pi} \text{ (see page 354),}$$

where E is the voltage and I the current measured on the continuous-current side of the converter, then, for any relative angular velocity $(\dot{a} - \dot{x})$, the torque exerted by the damper will be

$$b(\dot{a} - \dot{x}) = \frac{EI \times p \times (\dot{a} - \dot{x})}{9{\cdot}81 \times R_{ps} \times 2\pi \times 2\pi ns} \text{ kilograms at a metre radius.}$$

Now the torque required to produce an angular acceleration $\ddot{a}$ is

$$a\ddot{a} = \frac{\Sigma mr^2}{9{\cdot}81}\ddot{a} \text{ kilograms at a metre radius,}$$

and the synchronizing torque for a relative angular displacement $(a - x)$ is

$$c(a - x) = \frac{EI \times \beta \times p}{9{\cdot}81 \times R_{ps} \times 2\pi}(a - x) \text{ (see page 341).}$$

Here a is the angular displacement of the armature and x is the angular displacement of the vector representing the supply voltage relatively to a uniformly rotating vector. We may take $x = A \sin \omega t$, where A is the amplitude of displacement of the voltage vector.

In what follows, therefore, we may use the contractions,

$$a = \frac{\Sigma mr^2}{9{\cdot}81},$$

$$b = \frac{EI \times p}{9{\cdot}81 \times R_{ps} \times 2\pi \times 2\pi ns},$$

and

$$c = \frac{EI \times \beta \times p}{9{\cdot}81 \times R_{ps} \times 2\pi}.$$

THE PHASE-SWINGING OF SYNCHRONOUS MOTORS AND ROTARY CONVERTERS. WITH AND WITHOUT DAMPERS.

The problem of damping * of the phase-swing of a synchronous motor or rotary converter is rather different from that of the damping of oscillations of a generator set up by irregularities in the turning moment, because the torque due to the damper is not proportional to the angular velocity of the phase-swing $\dot{a}$, but to $(\dot{a}-\dot{x})$, where $\dot{x}$ is the rate of change of the angular displacement of the phase of the impressed voltage from a voltage of constant frequency.

We get (see page 601)

$$\mathrm{a}\ddot{a}+b(\dot{a}-\dot{x})+c(a-x)=0. \quad \text{.............................(1)}$$

If $x=A\sin\omega t$, the solution of the above equation is

$$a=-\frac{A\sqrt{c^2+\omega^2b^2}}{\sqrt{\omega^2b^2+k^2}}\sin\left(\omega t+e+\tan^{-1}\frac{\omega b}{k}\right), \quad \text{.....................(2)}$$

where $e=\tan^{-1}\dfrac{\omega b}{c}$, $\omega=2\pi n_d$, n_d is the frequency of the phase-swing, and $k=(\mathrm{a}\omega^2-c)$.

If $b=0$, that is, if there is no damping, $a=-\dfrac{Ac}{k}\sin\omega t$, and as $k=\mathrm{a}\omega^2(1-q)$,

$$a=-\frac{Aq}{1-q}\sin\omega t, \quad \text{where } q=\frac{c}{\mathrm{a}\omega^2},$$

That is to say, the original swing is multiplied by $\dfrac{q}{1-q}$. Where $q=1$, a will become infinite, if $b=0$ (see page 340).

It is interesting to enquire how great the swing will be where $q=1$, and the damping is such as one would find in practice. Writing $k=0$ in (2), we get

$$a=-\frac{A\sqrt{c^2+\omega b^2}}{\omega b}\sin\left(\omega t+e+\frac{\pi}{2}\right).$$

Now let us give values to a, b and c such as we might have in the 1250 K.W. rotary described on page 570, taking first of all a 3 per cent. damper (see page 354).

$$\mathrm{a}=\frac{1026}{9{\cdot}81}; \quad b=\frac{1{\cdot}25\times10^6\times7}{9{\cdot}81\times7{\cdot}15\times6{\cdot}28\times6{\cdot}28\times50\times{\cdot}03}=2{\cdot}1\times10^3;$$

$$c=\frac{1{\cdot}25\times10^6\times1{\cdot}4\times7}{9{\cdot}81\times7{\cdot}15\times6{\cdot}28}=2{\cdot}78\times10^4 \text{ kilograms at a metre.}$$

Here we have put $\beta=1{\cdot}4$. This is worked out in the method given on page 294.

The value of ω which will make $k=0$ is $\omega=16{\cdot}4$. That is, $n_d=2{\cdot}6$.

Then $\omega b=16{\cdot}4\times2{\cdot}1\times10^3=3{\cdot}44\times10^4$. $\sqrt{c^2+\omega^2b^2}=4{\cdot}4\times10^4$.

$$a=-1{\cdot}28\,A\sin\left(\omega t+\tan^{-1}\frac{\omega b}{c}+\frac{\pi}{2}\right),$$

$$\ddot{a}=268\times1{\cdot}28A\sin\left(\omega t+\tan^{-1}\frac{\omega b}{c}+\frac{\pi}{2}\right).$$

* "The Function of Damping-coils in the Parallel Running of Alternators," I. Döry, *Elektrot u. Maschinenbau*, 27, p. 315, 1909; "Theory of Damping in Parallel Running," C. F. Guilbert, *Lumière Électr.*, 9, pp. 355 and 387, 1910; "The Proportioning of Amortisseurs," Emde, *Electrot. u. Maschinenbau*, 27, p. 1073, 1910; "The Amortisseur Winding," M. C. Smith, *Gen. Elect. Rev.*, 16, p. 232, 1913.

The maximum value of $a\ddot{a} = 3{\cdot}6 \times 10^4 \times A$ kilograms at a metre.

The maximum value of $b\dot{a} = 2{\cdot}1 \times 10^3 \times 16{\cdot}4 \times 1{\cdot}28 \times A = 4{\cdot}4 \times 10^4 A$.

The maximum value of $ca = 3{\cdot}6 \times 10^4 \times A$.

The maximum value of the synchronizing torque is obtained by finding the maximum value of $c(a-x)$. The quantities take up the phase positions shown in Fig. 522, from which it will be seen that

$$c(a-x) = {\cdot}8cx, \qquad a - x = {\cdot}8x.$$

Now, how big may we expect to find x in ordinary practice? If a 50-cycle generator, supplying the network on which the rotary runs, having 64 poles, running

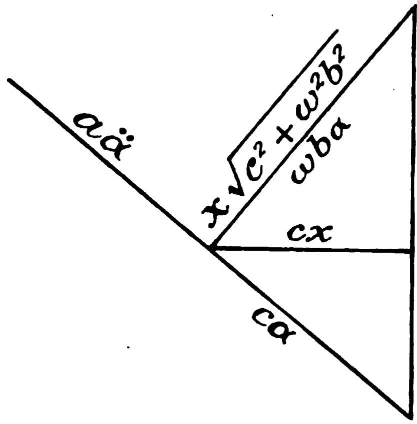


FIG. 522.—Phase positions of the various torques exerted on a damped synchronous motor when running on a circuit of varying frequency.

at 1·57 revs. per sec., has an angular irregularity of $\frac{1}{250}$th, we will have an angular variation of $\cdot004 \times 1{\cdot}57 \times 2\pi$ radians per second. This will give rise to an angular displacement of the phase of $\dfrac{\cdot004 \times 1{\cdot}57 \times 2\pi}{2\pi \times 2{\cdot}6} = \cdot00242$ radian on the generator, or $\cdot00242 \times \frac{64}{14} = \cdot011$ radian on the converter. So the maximum value of $x = \cdot011 = A$. But $a - x = {\cdot}8x$. Therefore the maximum synchronizing torque under the conditions will be:

$$c(a-x) = \frac{EI \times 1{\cdot}4 \times 7}{9{\cdot}81 \times R_{ps} \times 2\pi} \times {\cdot}8 \times {\cdot}011.$$

That is to say, the synchronizing torque may be about $1{\cdot}4 \times 7 \times {\cdot}8 \times {\cdot}011$ of full-load torque, or $8\frac{1}{2}$ per cent. This would be quite enough to affect the commutation.

If we make the damper of sufficient conductivity to reduce the slip to $1\frac{1}{2}$ per cent. at full load, we reduce the value of $c(a-x)$ to 3·8 per cent. of full-load torque.

That is to say, that the number of ampere-turns applied by the armature to the commutating pole in excess of the correct number at one part of the swing and subtracted from the commutating-pole excitation at another part of the swing is not more than $\cdot038 \times 7000 = 265$ (see page 599), and as the total number of effective ampere-turns on the pole is 4720, the interference with the commutation is only very slight.

We see, therefore, that if we fit very good dampers and have the commutating conditions so that the commutation is not sensitive, we can run satisfactorily even if $k = 0$.

SMALL ROTARY CONVERTERS.

The specification for a small converter should be as simple as possible, so as to enable a manufacturer to quote on his standard plant, and should be confined to a statement of the characteristics required, such as given in Clause 242, page 584, a statement of the purpose for which it is required, and only such special clauses as are absolutely necessary. The starting of small converters (up to 100 K.W. in small systems and up to 300 K.W. in large systems) is generally carried out by taps on the transformer without any starting motor, as shown in Fig. 507. For 500-volt machines, 3-phases are more common than 6-phase for small machines, because the extra complication in wiring and slip-rings is hardly worth while when the current per ring is very low. When running from C.C. to A.C. it is also usual to do without an exciter (see page 559), when the cost of this would be an excessive fraction of the total cost of the plant. A series winding will in most cases be sufficient to keep the speed of the converter within reasonable limits.

CHAPTER XX.

PHASE ADVANCERS.

THE phase advancer stands in the same relation to an induction motor as an exciter does to a synchronous motor. The exciter supplies a continuous current to magnetize the field-magnet, so that the power factor of the motor may be kept near unity. The phase advancer supplies a current slowly alternating with the frequency of the slip, which acts as a magnetizing current and obviates the necessity of any magnetizing current being supplied to the stator; and thus the power factor of the motor is improved. Where it is desired to run an induction motor on a leading power factor, the phase advancer is made to supply a magnetizing current greater than the normal magnetizing current, and it is then found that the stator draws a leading current from the line, just as a synchronous motor does when it is over-excited.

A phase advancer can, of course, only be used in conjunction with a motor which has a wound rotor with the ends of the windings brought out to slip-rings. Squirrel-cage motors cannot, therefore, be used with phase advancers.

It is only when the user of the motor has some interest in the power factor that he will go to the expense of installing a phase advancer. When power is charged for at so much per kilowatt-hour, independently of the power factor, the user of the motor will prefer to draw his magnetizing current from the line free of cost. But where an extra rate is charged for wattless current, or where a rebate is made for power taken at a good power factor (and it seems probable that in the future such systems of charging will be more common), it may be worth while for a user to supply his own magnetizing current. There are cases, too, where the user generates his own power, as, for instance, where a Corporation has large induction motor-generator sets on its mains. Here phase advancers could often be installed with advantage.

It will be chiefly in connection with large motors that these machines will be installed, because the cost of the extra appliances is too great a proportion of the cost of the motor where the motor is small.

In deciding whether or not it is worth while to install a phase advancer, the following matters should be taken into account:

(1) What monetary advantage is to be gained by improving the power factor of the motor?

(2) What will be the cost of the advancer and switch gear, and what extra power will it consume ?

(3) What extra attendance will be required ?

(4) What improved performance can be obtained from the motor ?

The strongest cases for the use of a phase advancer are those where the mains or the plant in the power house are loaded to their utmost, and it is desired to install more motors. In such cases the addition of a phase advancer of 10 K.V.A. capacity to one or two big motors may liberate some hundreds of K.V.A. to be used elsewhere, and obviate the necessity of extensive additions to the plant. The advantage which the advancer has in these cases arises from the fact that it supplies the magnetizing current at such a low frequency and low voltage that the total K.V.A. supplied is only a very small fraction of the wattless K.V.A. which would otherwise have to be supplied at the frequency and voltage of the station. The K.V.A. supplied by the phase advancer bears the same proportion to the K.V.A. saved in the stator as the slip of the motor bears to the synchronous speed.

The monetary advantage to be gained by improving the power factor can easily be found where the power is supplied by a public company, and definite rates are charged for true power and for wattless current. Where the user supplies his own power, it is not so easy to arrive at the cost of the wattless current. It would be necessary to make a close enquiry into the circumstances of each particular case. From a number of investigations made by Prof. Arno and Mr. Conti, engineers to certain Italian power companies, with respect to working cost of generation and transmission, as affected by the power factor of customers, it was found that the average cost was almost proportional to

$$\tfrac{2}{3}EI\cos\phi + \tfrac{1}{3}EI,\text{*}$$

so that perfectly wattless K.V.A. would cost one-third of the same K.V.A. at unity power factor.

When a power company has a large induction motor-generator, such as that described on page 448, running on its system, it would be possible, by means of a phase advancer, to run it on a leading power factor, so as to compensate for the bad power factor of other motors running on the same system. Instead of running at 0·88 power factor and drawing 640 K.V.A. wattless from the line, it might be made to run at a power factor of, say, 0·95 leading, and supply 460 leading wattless K.V.A. to the line, making a total change of 1000 wattless K.V.A. This would be sufficient to change a total load of 1660 K.V.A. at 0·8 power factor into a load of 1330 K.W. at unity power factor. If the cost of generation and transmission were as found by Arno, viz. proportional to $\tfrac{2}{3}EI\cos\phi + \tfrac{1}{3}EI$, the cost of a year's run of 3000 hours at 0·5*d.* per unit would be

$$(\tfrac{2}{3} \times 1330 + \tfrac{1}{3} \times 1660) \times 3000 \times 0{\cdot}5 = £9000$$

without the phase advancer, and

$$\frac{1330 \times 3000 \times 0{\cdot}5}{240} = £8350$$

with the advancer, giving a difference of £650. The total cost of a suitable advancer of 30 K.V.A. capacity, including the cost of extra copper in the rotor of the induction

* See Prof. Gisbert Kapp, *Inst. Elec. Engineers' Journ.*, vol. 50, page 351.

motor, would not be more than £250. The losses in the phase advancer would be about 6·5 K.W. (see page 570), and the saving in the efficiency of the induction motor 2·5 K.W., giving a total loss of 4 K.W., costing £25 per annum. If we estimate the extra attendance at £10 per annum, we make a saving of £615 per annum for a capital outlay of £250. This is on the basis of Arno's figures for the cost of wattless K.V.A., and the calculation would have to be modified to meet the circumstances of any particular case. Where the motor is not so large, the saving is not so great; but even down to sizes of 200 H.P. circumstances may be such as to make the addition of an advancer well worth while. At present the prices of these machines are very high, because the manufacturer regards them as special machines; but in the future the price will no doubt be very much reduced, and then smaller motors can be fitted economically.

In cases where a new motor is being supplied together with a phase advancer, the specification of the motor may vary in some respects from the specification of a motor taking its magnetizing current from the line. Specification No. 7A gives particulars of the variation which might be made in Specification No. 7 (page 438) to suit the case where a phase advancer is to be supplied with the motor.

SPECIFICATION NO. 7A.

1500 H.P. 3-PHASE INDUCTION MOTOR, 3000 VOLTS, 50 CYCLES, 246 R.P.M., INTENDED TO BE RUN ON LEADING POWER FACTOR.

This specification will contain all the Clauses in Specification No. 7, p. 438, with the following exceptions and additions:

Instead of Clauses 88 and 89, substitute the following:

Characteristics of Motor with Phase Advancer.

300. The motor shall have the following characteristics:

Normal output	1500 H.P.
Normal voltage at terminals	3000 volts.
Frequency	50 cycles.
Number of phases	3.
Speed	246 revs. per minute.
Power factor at full load	0·95 leading.
K.V.A. at no load	400 leading.
How connected to load	Direct connected through flange coupling.
How connected to phase advancer	Belted.
Size of pulley on motor shaft	37 inches in diameter. 10 inches on face.
Temperature rise after 6 hours full load run	40° C. by thermometer.
Over load	25 per cent. for 3 hours.
Temperature rise after 3 hours 25 per cent. over load	55° C. by thermometer.
Maximum torque	5 times full-load torque.
Puncture test	6600 volts alternating at 50 cycles applied for 1 minute between stator windings and frame. 4000 volts alternating at 50 cycles for 1 minute between rotor windings and frame.

301. The contract includes the delivery of the motor and phase advancer at the sub-station of the Corporation, together with bedplates, bearings and pedestals, and the erecting, aligning and coupling of the same to the 1000 K.W. generator, and the lining up and belting of the phase advancer. The switch gear and starting gear are provided for under another specification. Extent of Work.

302. After the rotor bars have been connected together, but before they are connected in star or in mesh, a pressure of 5000 volts shall be applied for 1 minute between phases A and B, B and C, and C and A. The phases shall then be connected in star or in mesh, and a pressure of 4000 volts shall be applied between copper and iron. This latter test shall be repeated after the motor has been run at full load for 6 hours, and while it is still hot. Tests on Rotor Coils.

In addition to Clause 100, there should be the following paragraph:

303. The tender shall state what further losses are occasioned in the motor when it is running in conjunction with the phase advancer under the conditions specified in Clause , and also the losses in the phase advancer itself and its connections. In the above statement the contractor is entitled to take credit for any diminution of losses in the stator winding or elsewhere. Efficiency.

Clause No. 102 will be modified by changing paragraph (3), which should read:

304. (3) Power factor test. During the temperature run the phase advancer shall be in circuit with the rotor winding, and the power factor of the combination shall be measured by means of a power-factor meter, and also by the two-wattmeter method. The motor shall also be run with the C.C. generator unloaded, and the power factor again observed, to see that the conditions described in Clause 300 have been met. Tests on Site.

The Clause as to brush gear will be the same as Clause 106, except that there should be added the following:

305. The slip-rings and brush gear shall be designed to carry the full-load current continuously without excessive heating or excessive wear. Brush Gear.

In specifying a phase advancer, the following particulars should be given:

(*a*) The type of motor to which it is to be fitted.
(*b*) The voltage and frequency of supply.
(*c*) The nature of the load: whether the motor is running continuously in one direction, or whether it reverses or stops often.
(*d*) The stand-still voltage of the rotor and the number of phases.
(*e*) The full-load working current.
(*f*) The power factor of the motor at full load and the no-load current.
(*g*) To what value it is intended to alter the power factor; or, in other words, what total change in wattless K.V.A. is desired.
(*h*) Particulars of the slip-rings and brush gear on the rotor, and their current-carrying capacity on continuous service.
(*i*) The method of starting the motor.
(*j*) The proposed method of driving the phase advancer. With high-speed motors it may be direct connected; with slow-speed motors it may be driven by a belt or connected to some other running machinery, or to an independent motor.

The following model specification shows how such particulars might be given:

SPECIFICATION NO. 16.

30 K.V.A. PHASE ADVANCER.

Rating of Motor.

306. The Phase Advancer is to be suitable for putting in circuit with the rotor of a 1500-H.P., three-phase, 3000-volt induction motor, running at 246 R.P.M.

Duty of Motor.

307. The motor will be direct-connected to a 1000-K.W. C.C. generator which will supply a power and lighting load. The motor will run 18 hours a day for the greater part of the year, with a load which will vary according to the conditions of service. The over load on the motor may amount to 25 per cent. for 2 hours.

Power Factor of Motor.

308. The calculated power factor of the motor is 0·88 at full load, and the no-load current about 90 amperes.

Desired Power Factor.

308*a*. It is desired that the motor shall be capable of yielding 400 leading wattless K.V.A. at all loads.

Standstill Voltage.

309. The rotor is wound for three phases, and the standstill voltage will be 1450 volts per phase.

Slip at Full Load.

310. The slip at full load will be about $1\frac{1}{4}$ per cent.

311. The full-load working current of the rotor will be 250 amperes. **Working Current.**

312. The slip-rings and brush gear of the rotor are designed to carry 400 amperes per ring in continuous service. **Slip Rings and Brush Gear.**

313. The motor will be started upon a water resistance, while the phase advancer is cut out. After the resistance has been short circuited, the phase advancer will be thrown into the circuit by means of a double-throw switch. **Method of Starting.**

314. It is proposed to belt the phase advancer to the motor.* **Driving Power.**

315. The above particulars are given for the guidance of the Contractor, but it is not intended that the amount of the leading wattless K.V.A. of the motor shall be limited to exactly 400 K.V.A., and so long as no overheating occurs, the wattless K.V.A. may with advantage be increased. **General Information.**

316. The brush-gear and commutator shall be amply designed so as to run continuously without overheating. **Brush Gear and Commutator.**

317. The temperature rise after 6 hours' full-load run shall not be more than 45° C. by thermometer. **Temperature Rise.**

318. The phase advancer shall be subjected to a testing pressure of 100 volts (alternating) applied for one minute between armature and frame and field coils and frame. **Puncture Test.**

319. With the phase advancer the Contractor shall supply a bedplate adapted for bolting to the bedplate of the driving motor, which is already existing, and particulars of which will be supplied. **Bedplate.**

320. Foundations will be supplied by the Purchaser to templates furnished by the Contractor. Cables from the motor to the throw-over switch, and from the throw-over switch to the advancer, will be supplied by the Purchaser, the throw-over switch being supplied under another contract. **Foundations. Cables.**

321. The contract will include the making of all proper connections and the setting to work of the complete plant. **Setting to Work.**

* In the case of high-speed motors it is convenient to direct-connect the phase advancer.

Losses. 322. The Contractor shall state the amount of the losses in the phase advancer when operating under the above specified conditions at full load.

Oil-throwing. 323. The journals, bearings and housings of the advancer shall be designed so as to be perfectly free from oil-throwing.

THE DESIGN OF A 30-K.V.A. PHASE ADVANCER.

For the general theory of phase-advancing the reader is referred to the specifications and articles* quoted below.

We propose here to give the method of designing a phase advancer to meet certain conditions of service. We will take the conditions set out in Specification No. 16.

The armature may either be of the open-circuit star type (see *Journal of the Institution of Electrical Engineers,* vol. 42, p. 612, Fig. 10, 1909), or of the closed-circuit type. Both kinds of armature commutate well. The first (see Fig. 523)

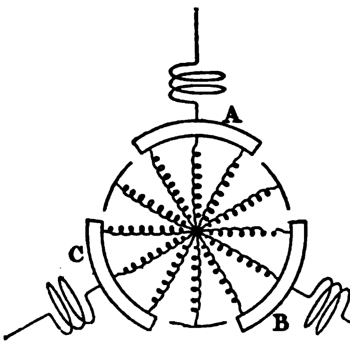


FIG. 523.—Diagram of open-circuit armature with several branches in parallel under wide brush belonging to each phase (see *Journal of the Institution of Electrical Engineers*, vol. 42, p. 612, Fig. 10, 1909.)

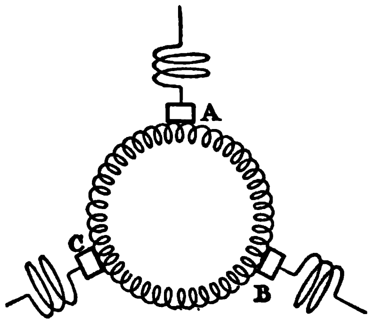


FIG. 524.—Closed-circuit armature forming a mesh connection between the phases.

is suitable when the current to be collected on the commutator is very great and the voltage to be generated is small, say not more than 15 volts. It enables a very wide brush (extending over 0·7 of the pole pitch) to be used. The second type

* Brit. Pat. Specification, No. 15,470 of 1895.

M. Walker, "The Improvement of Power Factor on Alternating-current Systems," *Journ. Inst. Elec. Engineers,* vol. 42, page 599; also *ibid.* vol. 50, page 329.

"Improving the Power Factor of Induction Motors," *Elec. Engineering,* 6, p. 229, 1910; *Electrician,* 64, p. 1064, 1910; "Phase Compensation of Induction Motors," Brit. Pat. 28,383 (1911), *Engineer,* 114, p. 507, 1912; "New Machine for Phase-compensation of Single- or Polyphase Induction Motors," A. Scherbius, *Elektrotech. Zeitschr.,* 33, p. 1079, 1912.

Dr. G. Kapp, "On Phase-Advancers for Non-synchronous Machines," *Electrician,* vol. 69, pages 222, 272.

"Improvement of Power Factor in Power Systems," Bauer, *Schweiz. elektrot. Verein,* Bull. 4, p. 304, 1913; "Theory and Applications of the Leblanc Exciter," Ehrmann, *Lumière Élect.,* 22, p. 291, 1913; "Improvement of Power Factor." Kapp, *Elektrot. Zeit.,* 34, p. 931, 1913; "Phase Compensation," Fynn, *Elec. World,* 62, pp. 28, 75 and 132, 1913; "Phase Variator," Campos, *Atti dell'Assoc. Elettr. Ital.,* 17, p. 221, 1913.

(see Fig. 524) is suitable when the current is not very great and the voltage is higher. As the current, in the present case, will only be a little over 300 amperes and the voltage to be generated will be about 70, we will choose the mesh-connected type.

In this case the rotor has a three-phase star-connected winding having a standstill pressure of 1450 volts per phase. The working current (that is to say, the current in phase with the voltage) will be about 260 amperes, which can be collected on a comparatively small collector. To find the rotor current necessary to make the motor run at 0·95 leading power factor, proceed as follows:

Set off a vertical line representing 260 amperes, as shown in Fig. 525. The power factor of the motor is 0·88, so that without the advancer one would have a lagging current equal to 47 per cent. of the working current. If the advancer caused the rotor to take a leading current of 47 per cent. (that is, 122 amperes),

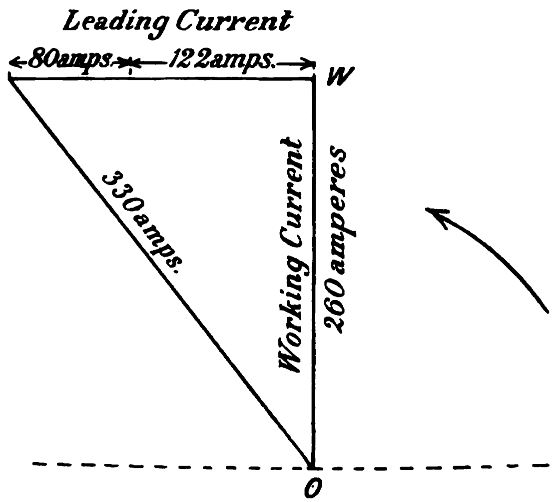


FIG. 525.—Construction for finding value of rotor current required to produce a given leading power factor.

the power factor at the stator terminals would be nearly unity. If, now, it is desired to make the power factor at the stator terminals 0·95 leading we must supply to the rotor an additional 31 per cent. of leading current, making 202 amperes wattless in all. Adding as vectors the 202 amperes wattless to the 260 amperes working current, we get 330 amperes per phase for the rotor when running under these conditions. This is the current for which the advancer must be designed.

Next, as to the voltage to be generated by the advancer. As the armature of the advancer is to be mesh-connected, it is simpler to take the voltages across the slip-rings than the voltage per phase of the star winding. Indeed, as the motor would work the same whether it were mesh-connected or star-connected, we may, if we like, consider it mesh-connected, as we have done in Fig. 527. If the normal slip of the motor at full load be 1·25 per cent., the E.M.F. generated by the slip will be 31 volts measured between rings. Lay off as in Fig. 526 the vertical line OE_a to represent this voltage generated by the slip in phase A. In Fig. 525 we have found the angle by which the current must lead on this voltage, so we can set off

the line Oa to represent the current in phase A (see Fig. 526). Similarly Ob and Oc represent the currents in the other phases. We should allow about 7 volts for

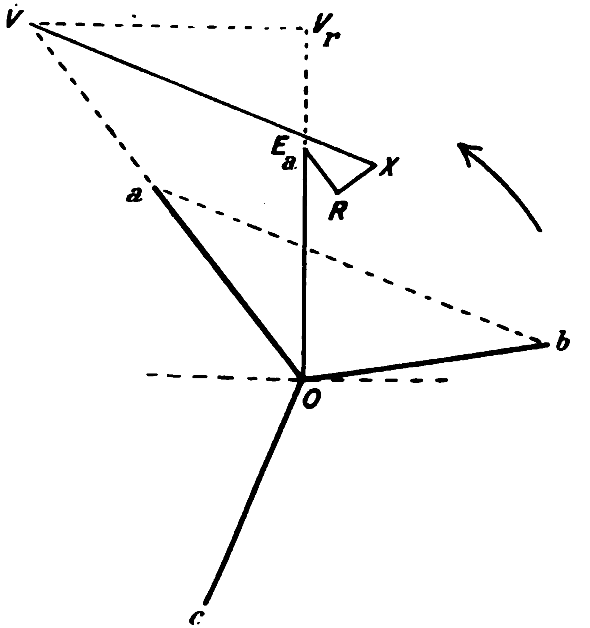


FIG. 526.—Construction for finding the voltage required to be generated by advancer.

pressure drop in brushes and in the resistance of the advancer. This will be represented by E_aR in phase with Oa. Then there will be some reactive drop in the field coils of the advancer. We may provisionally allow 6 volts for this, and after the machine is calculated we can make a check calculation to see if it is enough. This is represented by RX. There is no reactive drop in the armature, because the compensating winding wipes out its field. We see that, if we add a voltage XV, parallel to ba, we shall get a resultant voltage OV in phase with Oa; and this is what we want. If, therefore, we excite the advancer with a current which is in phase with the sum of Oa and $-Ob$ (shown by the dotted line ba), we can make the current lead by the right amount. The voltage to be generated by the advancer is therefore given by XV, which, when scaled off, gives us 49·6 volts. It will be seen that the projection of OV on the vertical line gives us OV_r, which is greater than OE_a.

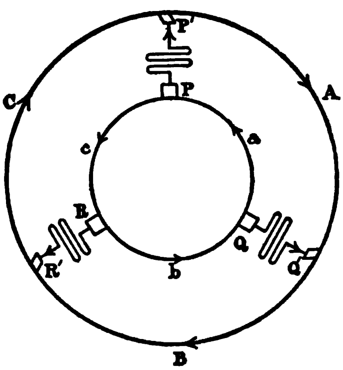


FIG. 527.—Diagram of mesh-connected phase-advancer armature a, b, c, field connections P, Q and R, and mesh-connected rotor A, B, C of induction motor.

If this voltage OV_r is greater than is necessary to drive the working current through the rotor circuit, the only effect will be that the slip of the rotor will be reduced until we get the right working current for the load. If it should prove

that OV_r is not sufficient to drive the working current, then the slip of the motor will be increased.

From Fig. 526 it appears that with 49·6 volts generated by the advancer the slip will be slightly reduced. We thus arrive at the rating of the advancer, namely, 49·6 volts between terminals and 330 amperes per phase.

We have next to decide how the advancer shall be excited. If the machine be excited by means of a series winding, it will have the general characteristics of a series-wound C.C. generator; that is to say, if the speed is high enough to make it excite when connected in circuit with a given resistance, it will immediately take a load sufficient to saturate the iron of the magnetic circuit, and the load will only be limited by the state of saturation. So that whether the induction motor is on load or not, the voltage generated by the phase advancer will be fairly constant,

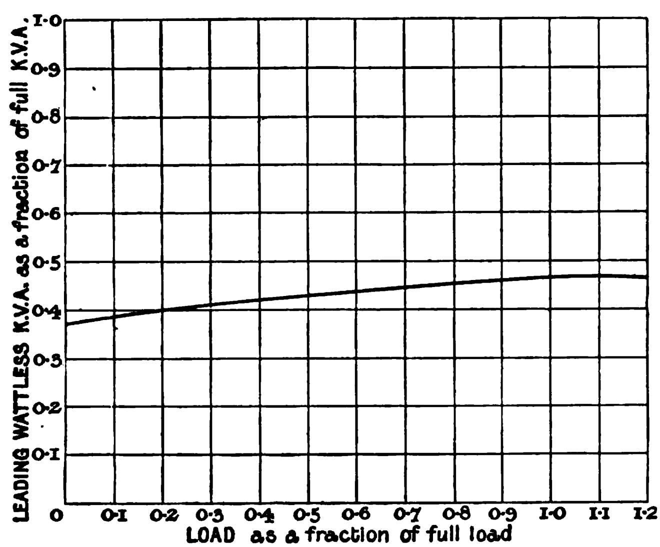


FIG. 528.

depending on the speed, and the flux which saturates the frame. When the motor is loaded, the E.M.F. generated in the rotor bars helps to increase the rotor current, and thus brings about a little more saturation of the frame; but this is only sufficient to make an unimportant increase in the leading current drawn by the stator from the line. The relation between the leading wattless K.V.A. and the load on the motor will, in fact, be as indicated in Fig. 528. The point at which the curve cuts the no-load line will depend upon the speed of the advancer and the resistance in series with it. In order to make the curve strike the desired point, 0·37 in our case, it is necessary to see that with 330 amperes flowing round the field coils we are generating the desired voltage.

As the characteristic given in Fig. 528 is the one which we desire, we will adopt a series winding in this case. If it had been necessary to control the power factor within narrow limits at all loads, we should find a separately-excited phase advancer would be more suitable.

Theoretically, three salient poles (equivalent to two magnetic poles) are quite enough for a machine of the rating required in this case, but a machine of six poles (equivalent to four poles magnetically) is more likely to fit in with standard frames and standard punchings. We will therefore decide on six poles. This will give us six brush arms, two in parallel in each phase. There will be 165 amperes per brush arm, and $165 \div 1{\cdot}73 = 96$ amperes per conductor.

It will not be worth while to cut down a machine of this type to the smallest possible size, because the addition of a little superfluous material will not increase the cost by a very large percentage, and when we are making a machine we might as well make it so that without much further development it may be used in a large variety of cases. If we take a large D^2L constant of 10×10^5 cubic cms., it will not be excessive, though very ample. A diameter of 46 cms. is suitable for a speed of 750 revolutions per minute, and the length of iron may be 19 cms.

The easiest way of designing a phase advancer of this type is to proceed as if it were a continuous-current machine whose voltage is 1·41 times greater than the virtual voltage called for in the specification. The armature need not differ in any particular from a continuous-current armature. The field winding will be provided with series exciting coils and compensating windings connected to the various phases in the manner described below.

The main points to look to, that are not found in a continuous-current design, are:

1. The machine, though having six salient poles, is a 4-pole machine magnetically, and we must remember this when fixing the dimensions of the iron behind the slots.
2. The voltage to be generated as a continuous-current machine is 1·41 times greater than the virtual voltage called for.
3. The fluxes in the salient poles which constitute magnetically a pole-pair are 120° apart in phase, so that the voltage generated in an armature coil, which lies partly under one pole and partly under another, is only 0·86 of the voltage that would be generated if the two poles were carrying the maximum flux at the same time.
4. It is necessary to arrange the series winding on each pole so as to cause the flux to be the right amount ahead in phase of the current carried by the armature conductors passing under the pole.
5. It is desirable to arrange the compensating winding so that its effect is equal and opposite to the armature winding adjacent to it, and for this purpose it is necessary to have regard to the phases of the currents in the armature and field.
6. It is desirable to provide a commutating flux which shall be proportional to, and in phase with, the current to be commutated.

The current loading. We begin, then, just as we would on a continuous-current generator. The voltage to be generated is $49{\cdot}6 \times 1{\cdot}41 = 70$ volts. There are six ways through the armature, each carrying 96 amperes. If we choose 72 slots with 4 conductors per slot, we get 288 conductors, and these multiplied by 96 give us 27,500 ampere-wires, a fairly easy current-rating for an armature 46 cms. in diameter.

PHASE ADVANCER.

Date 6th Jan. 1913. Type ~~GEN.~~ ~~SYN MOTOR~~ ~~ROTARY~~ 6 Poles =4. Elec Spec. 16
K V A. 29 ; P.F. ·2 ; Phase 3 ; Volts 50; 70 max ; Amps per ter. 330 ; Cycles ·65 ; R P M. 750 ; Rotor Amps ;
H.P. Amps p. cond. 96 . Amps p. br. arm. 165 Temp rise 40° C Regulation Overload

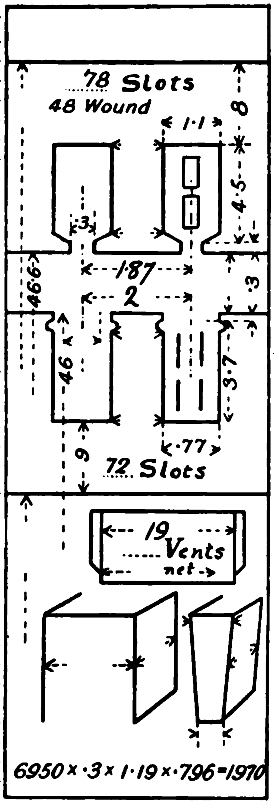
Customer ; Order No. ; Quot. No. ; Perf. Spec. ; Fly-wheel effect

Frame 46/19 Air ; Circum. 145 ; Gap Area 2760 ; poss A_g B ; A_g B 0·192 × 10⁸ ; poss. $I_a Z_a$; $I_a Z_a$ 27,500 ; $\frac{I_a Z_a}{\text{Circum.}}$ 190 ; $\frac{D^2\ L\ RPM}{K.V.A.}$ 10·4 × 10⁵ ;

K_a ·71 × ·86 ; 70 Volts = ·7 × 12·5 × 48 × ·192 × ·86 ; Arm. A.T. p. pole ; Max. Fld. A.T.

Armature. Rev. ~~Stat.~~

Core.	Dia. Outs.	46	
	Dia. Ins.	20·6	
	Gross Length	19	
	Air Vents 1	0·6	
	Opening Min. 1 Mean		
	~~Air~~ Velocity	18 m.p. sec.	
	Net Length 18·4 × ·89	16·4	
	Depth b. Slots	9	
	Section 139 Vol.	20,000	
	Flux Density	8100	
	Loss ·02 p. cu. cm. Total	400	
	Buried Cu. 350 Total	660	1010
	Gap Area 2760 ; Wts	550	
	Vent Area 1800 ; Wts	150	
	Outs. Area 2500 ; Wts	380	1080
Teeth.	No of Segs 1 Mn. Circ.	127	
	No of Slots 72 × ·77 =	55·5	
	K_s	61·5	
	Section Teeth	1010	
	Volume Teeth	3750	
	Flux Density	19,000	
	Loss ·07 p. cu cm Total	260	
	Weight of Iron	185 kilogrs.	
Conductors.	~~Star or~~ Mesh Throw	1-12	
	Cond. p Slot	4	
	Total Conds 48 in series	288	
	Size of Cond. ·23 × 1·27	·28 sq. cm.	
	Amp. p. sq. cm	310	
	Length in Slots 19		
	Length outside 35 Sum	54	
	Total Length	1560	
	Wt. of 1,000 24·5 Total	380 kgs	
	Res. p. 1,000 ·608 Total	·095/36	·0044
	Watts p.		
	Surface p.		
	Watts p. Sq.		



Field Stat ~~or Rotor.~~

~~Dia.~~ Bore		46·6	
½ Total Air Gap		·3	
Gap Co-eff. K_2		1·19	
Pole Pitch Pole Arc		16·7	
K_f		0·7	
Flux per Pole		2·25 × 10⁶	
Leakage n.l. f.l.			
Area 131 Flux density		8750	
Unbalanced Pull			
No. of Seg. 1	Mn. Circ.	154	
No. of Slots 78	× 1·1 =	86	
Vents		68	
K_s Section		1110	
Weight of Iron		230 kilogrs.	

	Shunt.	Series.	Comm.
A.T. p Pole n. Load		0	0
A.T. p. Pole f. Load	max.	2800	2800
Surface			
Surface p. Watt			
I² R.		920	920
I. R.		·95	·95
Amps.		330	330
No. of Turns	Equiv. of	6	6
Mean l. Turn		106	106
Total Length		50	50
Resistance		·0085/3	·0085/3
Res. per 1,000		·17	·17
Size of Cond.		·7 × 1·5 =	1 sq. cm.
Conds. per Slot		16	2
Total		96	96
Length		50	50
Wt. per 1,000		890	890
Total Wt.		45	45
Watts per Sq.			
Star ~~or Mesh~~			
Paths in parallel		1	1

Magnetization Curve.

	Section	Length	70 Volts. B.	A.T. p. cm.	A.T.	Volts. B.	A.T. p.	A.T.	Volts. B.	A.T. p.	A.T.
Core	139	10	8100	2	20						
Stator Teeth	1110	4·5	17,300	64	290						
Rotor Teeth	1010	3·7	19,000	135	500						
Gap	2760	·3	6950		1970						
Pole Body											
Yoke	131	13	8750	2·3	30						
					2810						

Commutator.

Dia. 30 Speed 12 m.p. sec
Bars 144
Volts p. Bar 3
Brs. p. Arm 4
Size of Brs. 2 × 4·5
Amps p. sq. 4·6
Brush Loss 720 + 1300
Watts p. Sq. 0·25

EFFICIENCY	1¼ load.	Full.	¾	½	¼
Friction and W		·8			
Iron Loss		·7			
Field Loss		1·84			
Arm. &c. I²R		1·02			
Brush Loss		2·00			
		6·36			
Output					
Input					
Efficiency					

Mag. Cur. Loss Cur.
Perm. Stat. Slot
,, Rot. Slot × =
,, Zig-zag
2 × × =
1·77 × =
End × × =
Amps ; Tot.
τ = ; X_a =
S_1/S_2 ; r_a = +

Imp. √ + =
Sh. cir. Cur.
Starting Torque
Max. Torque
Max. H.P.
Slip
Power Factor

If we denote the area of the cylindrical working face of the armature by A_g and the maximum flux-density in the gap by B, then we get the magnetic-loading equal to A_gB. If we have a pole arc equal to 0·7 of the pole pitch, then, as there are 48 conductors in series and the speed is 12·5 revolutions per second,

$$70 \times 10^8 = 0{\cdot}7 \times 12{\cdot}5 \times 48 \times A_g\text{B} \times 0{\cdot}866.$$

Observe the multiplier 0·866, which comes into the equation on account of the circumstance mentioned in paragraph (3) above.

Thus we arrive at the **magnetic loading** $A_g\text{B} = 0{\cdot}192 \times 10^8$. If we work the iron in the teeth at 19,000 lines per sq. cm., we shall require a total mean cross-section of all the teeth of 1010 sq. cms. Our conductors, to carry normally 96 amperes and 25 per cent. over load, may be made 0·23 by 1·27 cms. Four of these, arranged as shown in Fig. 533, will require slots about 0·77 × 3·7 cms. To provide room for 72 slots and give the necessary cross-section to the teeth, we shall require a net length of iron of 16·4 cms. Allowing 11 per cent. for paper on the punchings and 0·6 cm. for a ventilating duct, we arrive at a gross length of iron of 19 cms. The rest of the calculation of the armature is the same as for a continuous-current machine, except in the matter of commutation, which we will consider later. The calculation sheet is given on page 617. The methods of obtaining the saturations, iron losses, and cooling conditions are the same as those described on pages 320 and 324. Figs. 529 and 533 give drawings of the machine to scale.

The series winding. We must now consider how we are to wind the field poles so as to give to the excitation its proper phase. The first point to note is that the six armature circuits are connected in mesh, while the leads from the brush holders are connected in star.

In Fig. 527 we have a diagram of connections as they would be if the machine had only three brushes. Obviously this diagram applies equally well to the machine with six brushes, where brushes at opposite ends of a diameter are in parallel with one another. The inner circle of Fig. 527 represents the closed winding of the armature of the advancer. The small letters a, b, c show the three phases mesh connected. Three brushes—P, Q and R—bear on the commutator and convey the currents to the outer circle, A, B, C, which represents the winding of the rotor of the induction motor taken as mesh-connected. It does not matter in practice whether the rotor of the induction motor is star- or mesh-connected, but for our diagram it is convenient to connect it in mesh. The arrowheads show the direction along each conductor, which is taken as positive for the purpose of our clock-diagram (Fig. 526). P, Q and R are in star, and it is only in series with them that we can connect the series exciting coils. The voltage in phase A of the rotor is the voltage we should measure by connecting a voltmeter to the collecting brushes P' and Q'. In order to make the current in this phase lead, it is necessary to generate a leading electromotive force in the part a of the armature circuit. From Fig. 526 we found that a suitable E.M.F. to inject into phase A was the E.M.F. XV, which is in phase with $(a-b)$. From Fig. 527 we see that the current in Q is $(b-a)$, so that $-Q$ is $(a-b)$. We will therefore excite the poles under which coils a are passing with $-Q$. The span of the armature coils is almost a pole pitch, so that the coils in phase a will be passing under two adjacent poles, which we will call pole P'' and pole

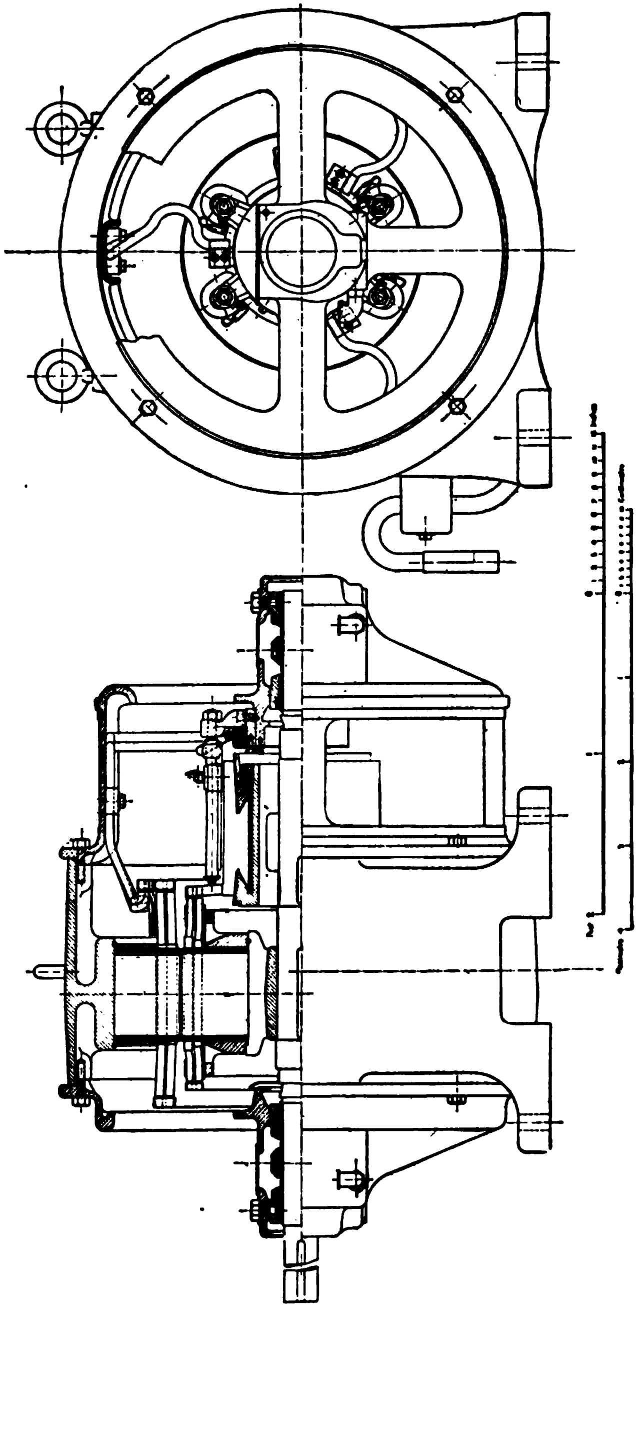


FIG. 529.—Sectional elevation of phase advancer.

Q'' (see Fig. 530). Now it is not convenient to use only the conductor Q to excite P'' and Q'', because we have to arrange for return paths and also for a compensating winding, and we want to make a fairly simple mechanical arrangement of the coils. We therefore take advantage of the known fact that currents

$$P+Q+R=0; \quad \therefore \; Q=-P-R.$$

Let us make an arrangement of exciting windings and compensating windings like that indicated in Fig. 530. There the exciting conductors which pass between poles P'' and Q'' are $+Q$, $+Q$, $-P$, $-R$. That is to say, they are equivalent to

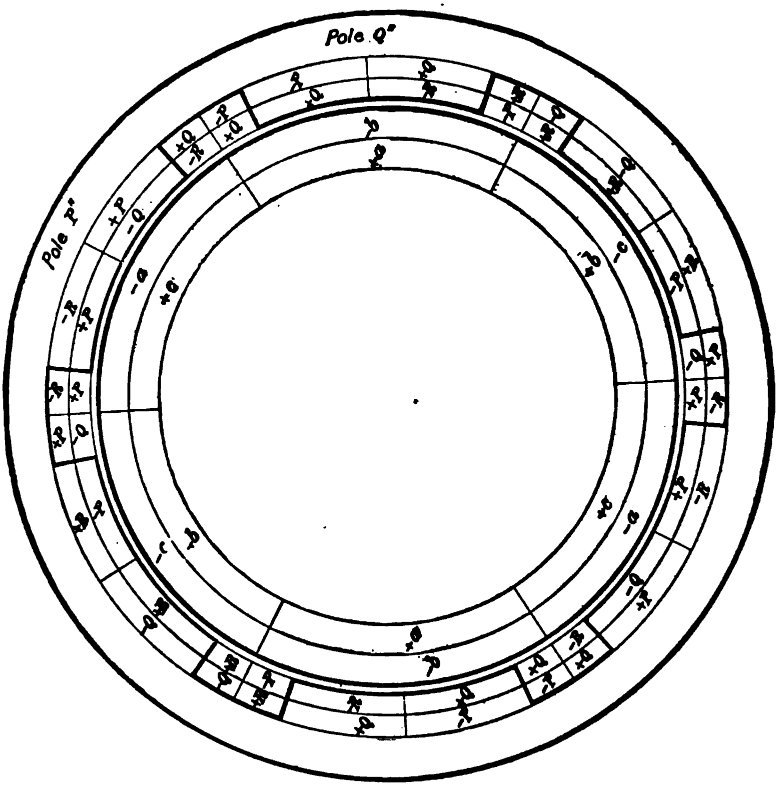


FIG. 530.—Showing relation of exciting windings and compensating windings to armature windings.

$3Q$. The question whether the excitation $+Q$ gives a forward or a backward E.M.F. in a coil depends upon the direction of rotation, and also upon the question whether the armature is wound right-handedly or left-handedly. It will be seen that this arrangement of conductors lends itself to form mechanically a simple barrel winding as shown in Fig. 531. The conductors lie in two layers, and all the end connectors of one layer are bent to the right, and all the end connectors of the other layer are bent to the left. This figure seems fairly complicated, but is made up by connecting, according to the scheme of Fig. 530, a number of groups of coils forming part of a simple barrel winding. Fig. 532 shows more exactly how the end connectors are arranged.

Compensating windings. The letters in Fig. 530 which are placed on the salient poles represent the compensating windings. It is easy to prove that these are in direct opposition of phase to the currents in the armature under the pole. For instance, take the pole P''. The compensating winding on this is

$$+P+P-R-Q, \quad \text{or} \quad +3P.$$

Now the armature coils which lie under P'' are c and $-a$, and we know that $a-c=+P$. Moreover, the 16 conductors in the pole face carrying the currents P, Q and R are equivalent to 12 conductors carrying the P current. Opposite the pole P'' are 12 armature slots each carrying $-2a$ and $2c$. When we remember that there are two paths in parallel per phase in the armature we see that the currents in these 12 slots are exactly balanced magnetically by the $12P$ currents in the compensating winding.

It will be found that an air-gap of 3 mms. will have an apparent length of 3·6 mms. when we take into account the opening of the slots. The flux-density in the gap obtained by dividing $A_g\text{B}$ by A_g is 6950; so that the ampere-turns on the gap will be 1970. The ampere-turns on the armature teeth will be 500, on the stator teeth 290, and on the rest of the magnetic circuit about 50; so that the ampere-turns per pole will be about 2800 or 5600 per pair of poles. These ampere-turns are provided by the 16 conductors which thread between the poles P'' and Q'', for the 16 conductors carry current equivalent to $3\times 4P$. At its maximum P is $330\times 1{\cdot}41$ amperes, which, multiplied by 12, gives us 5600 ampere-turns per pair of poles. In practice it will be found unnecessary to adjust the speed exactly, because the particular power factor at which the motor runs is not a matter of importance.

Fig. 531.—Development of series windings and compensating windings showing connections to the brushes.

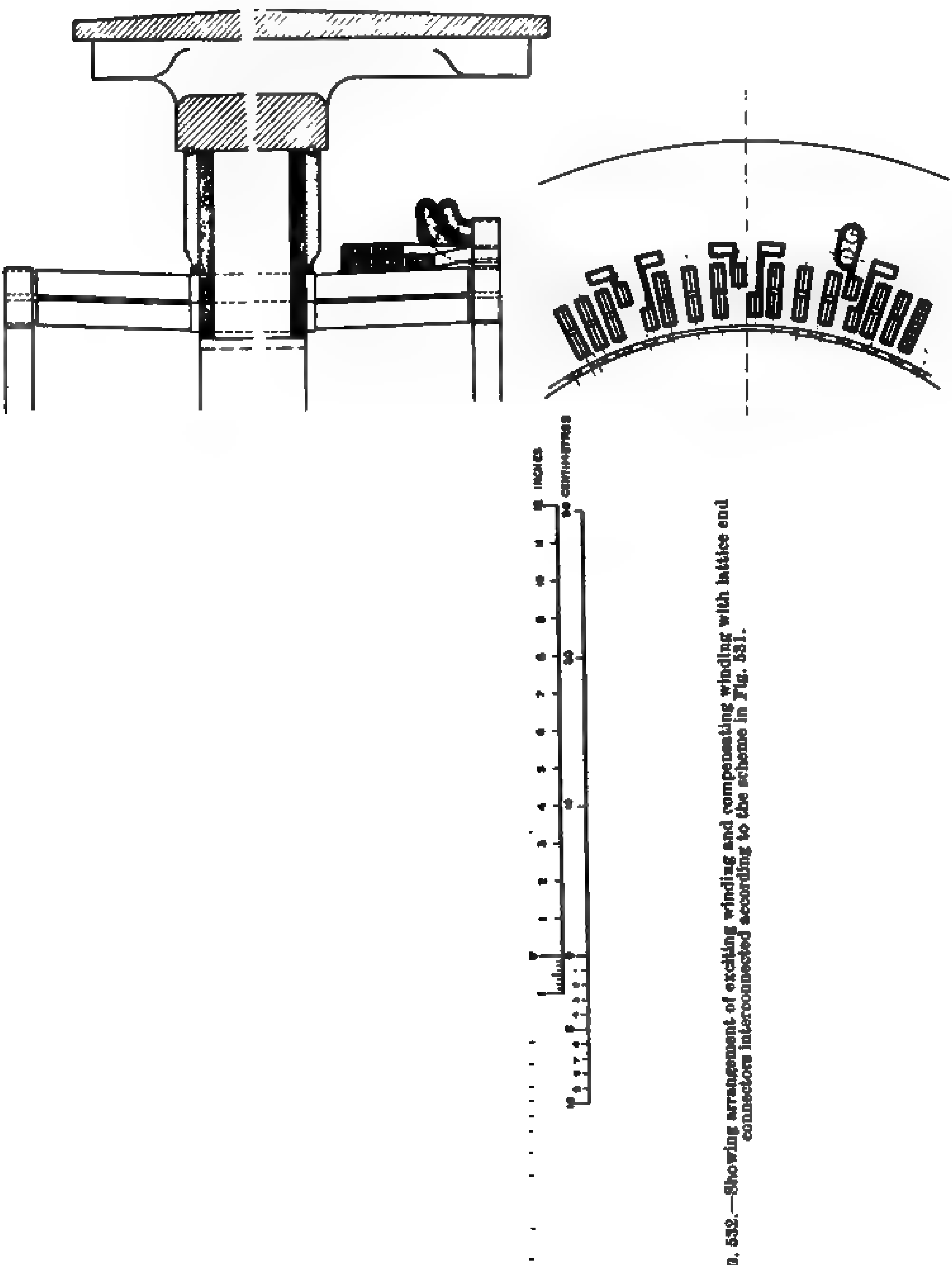


FIG. 532.—Showing arrangement of exciting winding and compensating winding with lattice end connectors interconnected according to the scheme in Fig. 531.

It is not usually necessary to make any provision for the adjustment of the power factor during running; it is sufficient that the motor shall take a leading current from the line at all loads. If it should be necessary to adjust the power factor, this can be done either by changing the speed of the advancer or by diverting some of the field current from the series coils.

Commutation. The most important consideration of the design of the phase advancer is the obtaining of good commutation. It is chiefly for this purpose that the field frame and winding described in this paper are provided. Where in a continuous-current generator the voltage between the bars is small, the commutation can generally be forced by the resistance of the carbon brushes; but it is very much more desirable to provide a commutating E.M.F. which shall at all times be proportional to the current to be commutated. In the machine here described this result has been effected by giving each armature coil a span of somewhat less than the full pitch and arranging the positions of the brushes so that one of the limbs of each coil is moving in the fringing field of a pole excited by a current which is at all times proportional to the current under commutation. The currents in the two branches of the armature, a and $-c$, which combine to form P, are out of phase with one another, and are not directly under control of the commutating flux; but the rate of change of the current in the coil under commutation ought at all times to be proportional to P. Now the pole P'' (Fig. 530) is excited so that the fringing field in which the left-hand limb of the coil a is moving is at all times proportional to P. By making the coil with a short throw the right-hand limb can be taken out of the influence of the pole Q''. The exact position for the brushes is, of course, obtained by trial; in practice it is found that the commutation is perfect. The alternation of the current in the armature and field causes a harmful E.M.F. to be set up in each coil under commutation; but as the frequency is so very low (say one cycle per second), this E.M.F. is not sufficiently great to create any disturbance. In the machine under consideration it only amounts to one-fourth of a volt.

CHANGE OF SPEED OF INDUCTION MOTORS.

Another use to which these exciters for the rotors of induction motors can be put is the changing of the speed over a wide range without the necessity for wasteful rheostats.

In order to change the speed of an induction motor, all that is necessary is to make the injected E.M.F. XV in Fig. 526 more in phase with the E.M.F. OE_a. If the injected E.M.F. is in the same direction as OE_a, the speed will be increased; and if it is opposed to OE_a the speed will be decreased. The generation of an injected E.M.F. in phase with OE_a is effected by arranging the series coils so that they carry a component of the current OC. This matter is discussed in the article referred to below.* At the same time that the speed is increased or diminished, the power factor can be improved by having a component of the injected E.M.F. at right angles to OE_a.

* *Journ. Inst. Elec. Engineers*, vol. 42, page 599.

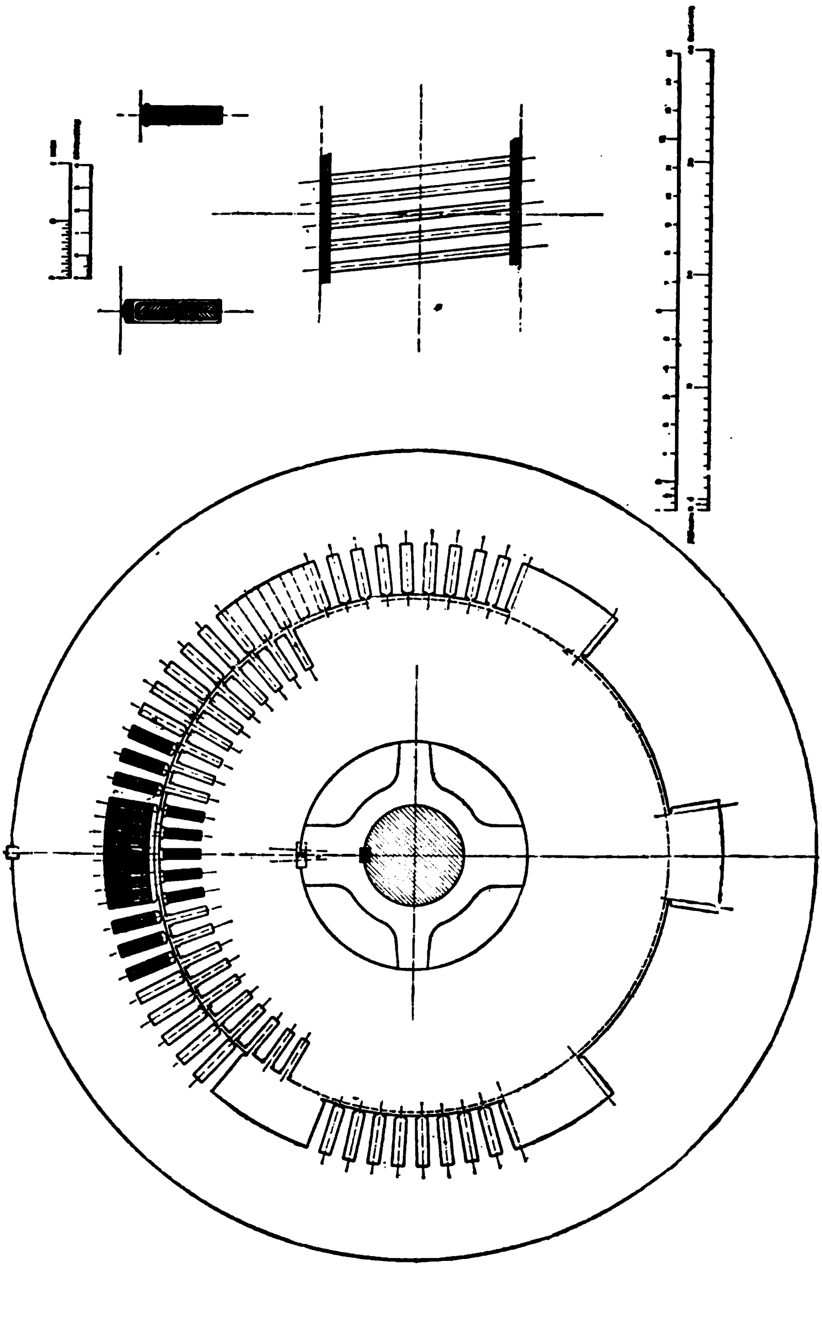


FIG. 533.—Showing slots of rotor and stator and the skewing of the slots in the rotor.

There are several other systems * of improving the power factor and changing the speed of induction motors which are of great interest; but as the matter of this book has already been extended beyond the limits originally planned by the publisher, there is not room to consider them here. It is hoped that the author may have an opportunity of treating in another volume of these and other developments in the application of dynamo-electric machinery to industrial purposes.

* "Methods of varying the Speed of A.C. Motors," G. A. Maier, *Amer. I.E.E.*, Proc. 30, p. 2511, 1911; "Speed Regulation of 3-Phase Motors," G. Meyer, *Elekt. Kraftbetr. und Bahnen*, 9, pp. 421, 453 and 461, 1911; "Adjustable-speed Polyphase Motors," Knöpfli, *Schweiz. elektrot. Verein*, Bull. 4, p. 185, 1913.

INDEX OF THE CLAUSES IN THE SPECIFICATIONS.

(*The first number gives the clause, the second the page.*)

GENERAL INDEX

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